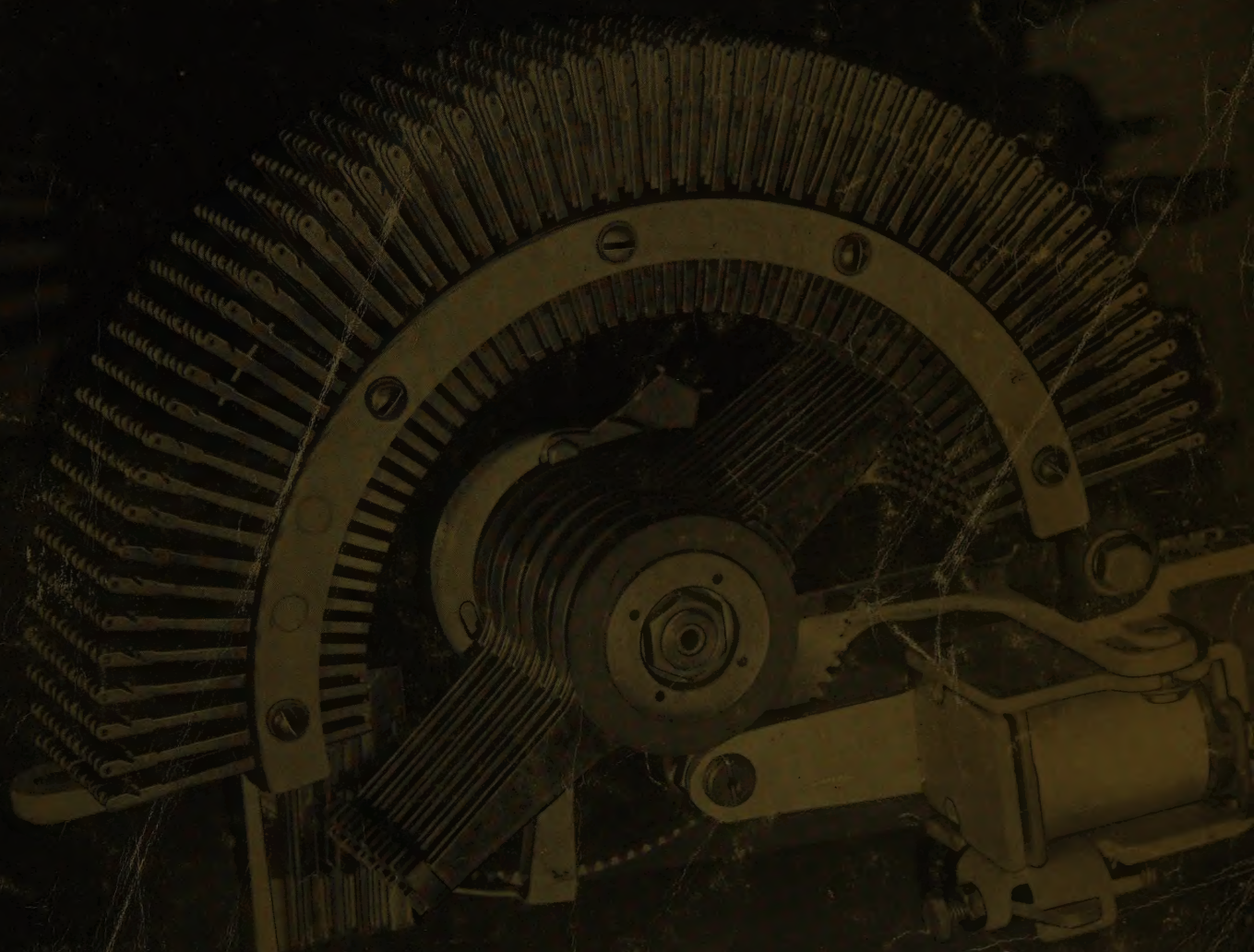
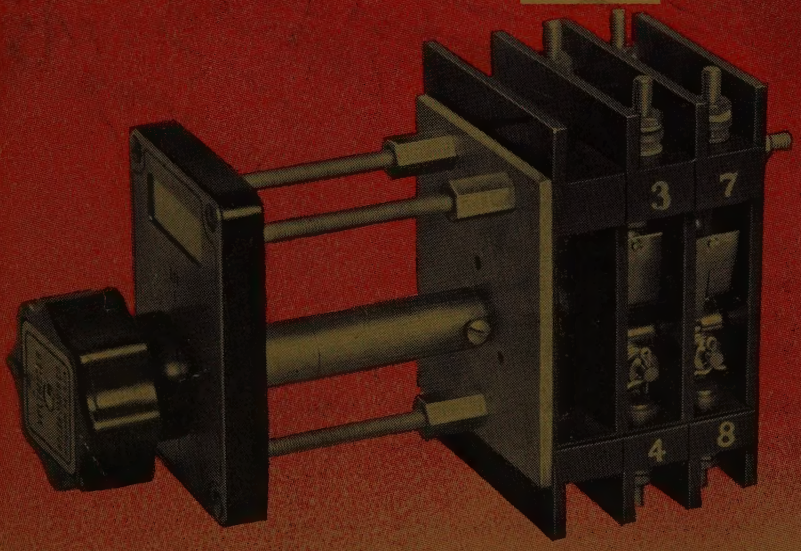
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The Cover: A switching device employed in dial telephone systems, this Federal Telephone and Radio 100-point finder automatically locates and connects with either an available or a preselected circuit.

International Telephone and Telegraph photo

The Selenium Rectifier.....Glen Ramsey . . . 425
The Centenary of the Morse Telegraph.....H. W. Bibber . . . 433
Electrically Heated Homes.....Buford H. Martin . . . 437
The ECPD Proposed Canons of Ethics.....Dugald C. Jackson . . . 441
Electrical Engineering in the Postwar World—VIII.....L. W. Chubb . . . 443
Miscellaneous Short Items: Searchlight Supplements Radio Communication, 440
Apparatus Imitates Sea Motion, 444
Institute Activities..... . . . 445
Of Current Interest..... . . . 452

TRANSACTIONS SECTION

(Follows EE page 460; a preprint of pages 869–960 of the 1944 volume)

British System of Cockpit Lighting.....Edward S. Calvert . . . 869
Considerations in Servomechanism Design.....S. W. Herwald . . . 871
Design Considerations of 400-Cycle Aircraft Motors.....M. B. Sawyer, Sr. . . . 877
Determination of Current-Transformer Errors.....E. C. Goodale, J. I. Holbeck . . . 879
D-C Arc Interruption for Aircraft.....J. S. Quill, L. T. Rader . . . 883
Selection of Aircraft Generators and Batteries.....L. M. Cobb, H. M. Winters . . . 889
Rotary Magnetic Clutch and Aircraft Electric Motors...Leo Andrews, Fred Shanely . . . 893
Electric-Wiring Installations in British Aircraft.....H. J. Horn . . . 896
Airline Aircraft Electric-Equipment Maintenance.....W. A. Petrasek . . . 900
Electric Connections on Aircraft.....F. O. Stebbins, L. A. Taylor . . . 906
Solenoid-Operated Control Valves.....V. W. Eckel, O. H. Wisegarver . . . 911
A New High-Frequency Capacitor.....W. M. Allison, N. E. Beverly . . . 915
Analysis of High-Frequency Ignition Circuits.....A. W. Robinson, Jr. . . . 916
Overheating Protection of D-C Aircraft Motors.....Cyril G. Veinott . . . 920
Historical Development of Electric Connectors.....Edward J. Neifing . . . 925
Testing of Brushes Under Various Altitude Conditions.....C. J. Herman . . . 929
Solderless Terminals.....F. H. Wells, J. C. Balsbaugh . . . 933
Electric Control in Automatic Pilots.....C. M. Young, E. E. Lynch, E. R. Boynton . . . 939
Peak Voltages With D-C Arc Interruption.....Virgel E. Phillips, Walter P. Mitchel . . . 944
Very High-Frequency Radio-Noise Elimination.....T. B. Owen . . . 949
Altitude Rating of Electric Apparatus.....Paul Lebenbaum, Jr. . . . 955

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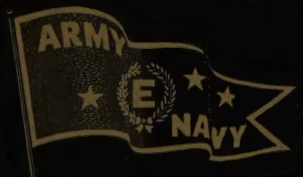
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The Selenium Rectifier

GLEN RAMSEY
MEMBER AIEE

A concise explanation of the construction and theory of operation of the selenium rectifier is combined with a comprehensive discussion of the effects of various applications on design by the author.

ALTHOUGH the selenium rectifier has been hailed by some as a new development, the photoelectric and rectifying properties of selenium were discovered many years ago. In 1932 selenium rectifiers were being used widely in Europe, and it was in that year that Fansteel Metallurgical Corporation began investigating the possibility of selenium rectifiers for railway signal service. In 1938 commercial production of selenium rectifier plates in the United States was commenced by a subsidiary of the International Telephone and Telegraph Company. The following year Fansteel selenium rectifiers, using International Telephone and Telegraph rectifier elements, were developed and introduced to railway, utility, and industrial users.

Railway signal service is an unusually rigorous test of a rectifier. In this service rectifiers usually operate continuously; located in signal cases along the right of way they are exposed to extremes of summer and winter temperatures, vibration from passing trains, moisture, and moderately corrosive atmospheres. Several thousand selenium rectifiers are operating under these conditions with complete satisfaction.

This article is based upon an intensive study of selenium-rectifier elements, both in the field and in the laboratory, by the author and his associates of the Fansteel engineering staff.

NOMENCLATURE

For clarity, the following terminology is used:

Plate: A single rectifier plate which in itself may be operated as a rectifier.

Element: An assembly of rectifier plates in series, parallel, series–parallel, center-tap or bridge circuit. Rectifier elements usually are made by assembling plates upon a center bolt or stud from which the plates are insulated. Rectifier elements also are called "stacks."

Fin: A square or round metal plate which performs no electrical function but is assembled in the element for the purpose of dissipating heat.

Rectifier: May be a single plate or element, a group of elements, or a complete assembly with transformer and controls.

Valve: A rectifier whose function is not to rectify a-c power but to block the reverse flow of direct current.

Ambient temperature: In general this term refers to the air temperature immediately surrounding a rectifier element. In this article where ambient temperatures are given, they are the temperatures of the air *below* the element, before it has been heated by the rectifier.

The advantages of the selenium rectifier make its use desirable in a very wide variety of applications wherever any type of dry-plate or metallic rectifiers may be used. High operating voltage per junction makes for smaller elements or assemblies of elements. At present selenium plates are being made that will operate at 18 and 30 volts per junction. Blocking layers have been formed to withstand considerably higher voltages. However, they are not available for commercial use. The high current density at which selenium rectifiers may be operated for normal rating, as well as higher efficiency which these factors produce, also contributes to their advantage. Wide temperature ranges can be withstood, a feature which is absolutely necessary in aircraft applications. Selenium rectifiers have long life and low aging properties. As a result of these advantages, the selenium rectifier is well established in the dry-plate rectifier field.

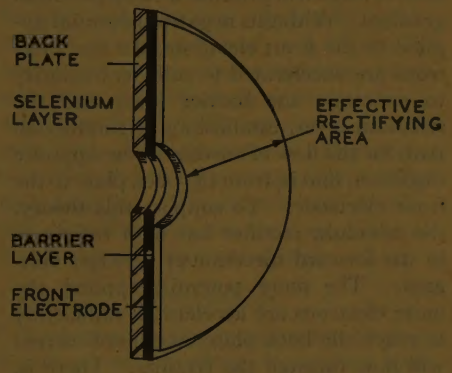


Figure 1. Cross section of selenium-rectifier plate

The field for good dry-plate rectifiers has expanded tremendously with the fast development of electronics resulting from the war. The selenium rectifier has filled these requirements, and, in most cases it is the only rectifier of its type that will meet them. Although we already have applied selenium rectifiers and valves to many new "impossible" tasks, there is no reason to believe that we shall not continue to find more and more uses for them. Exigencies of the war have brought about fundamental changes in engineering philosophy with the result that difficult problems of application now are approached with more confidence.

CONSTRUCTION

The selenium-rectifier plate consists of a nickel-plated steel or aluminum back plate (other metals also may be used), a thin layer of metallic selenium, a barrier layer, and the front electrode (see Figure 1). The back plate acts only as a support. The barrier layer is formed between the selenium and the front electrode. Current flows freely from the back plate to the front electrode but is blacked in the reverse direction. Suitable contact to carry away the current is made to the front electrode by a spring washer or other means (see Figure 2). The selenium rectifier is a one-piece rectifier requiring no high-pressure contact for rectification. As a matter of fact, high pressure on the barrier layer is detrimental. In normal construction, no front electrode is applied to the center of the plate and, subsequently, no barrier layer formed. This permits pressure to be applied to the spacing and limiting washers without affecting the reverse resistance.

A center contactor[6] has been developed whereby the spring (petal) washer is eliminated and a solid contact washer is used (see Figure 3). Contact is made to an area in the center which is covered with an

Glen Ramsey is manager of the rectifier division of the Fansteel Metallurgical Corporation, North Chicago, Ill.

The author expresses thanks to the following of his associates for assistance in compiling the information given in this paper: C. E. Holmes, Paul C. Dolken Stanley Fry, and James H. Hall.

insulator and additional front electrode over the insulated area. In the forming process no barrier layer is formed at this point, and no change in reverse resistance will result from pressure exerted on the assembly.

Plates are assembled on bolts in the proper order for the rectifier assembly required. As many as 24 to 40 plates may be assembled on one bolt, depending on size of plate and the plate spacing. Plates sizes vary from less than one inch in diameter to $4^3/_8$ inches. Other plates larger or smaller are made for special applications.

Selenium-rectifier plates are insulated from the center bolt by insulating tubing. In regular production, elements having 90 or less volts output are insulated for 600 volts alternating current. For output voltages above 90 elements are insulated for 1,500 volts alternating current. Insulation on the elements alone can be furnished up to 3,000 to 4,000 volts. Further insulation generally is accomplished in the mounting brackets.

For explanation purposes the selenium rectifier may be considered as a resistance

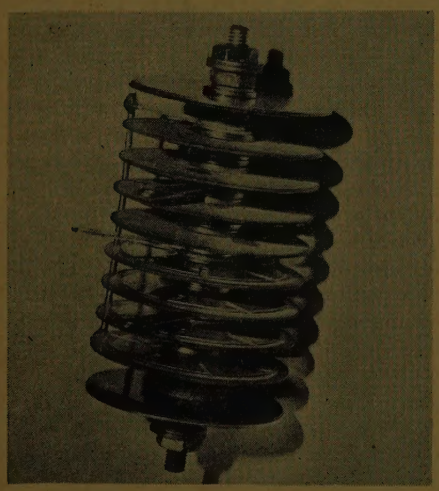


Figure 2. Selenium rectifier with spring washer

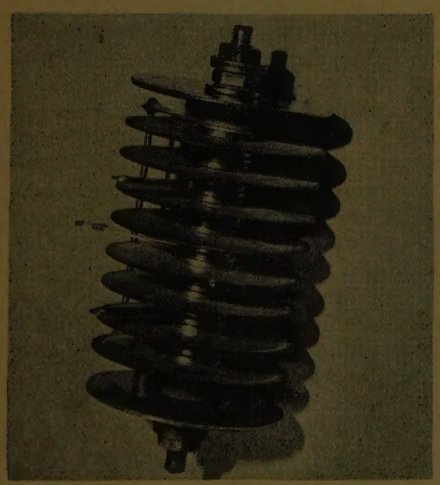


Figure 3. Selenium rectifier in which the spring washer has been replaced by a solid contact washer

device which has a very high resistance in one direction and a low resistance in the other. Current passes freely in the forward direction (low-resistance direction), but only a very small current will flow in the reverse direction under normal operating conditions.

THEORY OF OPERATION

The next logical question is: "What causes these asymmetrical characteristics"? In other words, "What makes it a rectifier"? Because of divided opinions on this phenomenon no attempt will be made to offer any new theory. One of the best and most logical explanations has been given by E. A. Richards,[4] which follows.

Any dry-plate or metallic rectifier must consist essentially of a semiconductor, a barrier layer, and a good conductor, the barrier layer being an insulator through which electrons can pass in either direction.

In the selenium rectifier, the semiconductor is the selenium, the front electrode is the good conductor, and the barrier or insulator layer is formed between the two. If the selenium rectifier is compared to a cold-cathode-emitter rectifier, the front electrode is the cathode, the back plate the anode, and the selenium and barrier layers the insulator between the two.

Considering the electron theory, the action can be described as follows: In the good conductor (front electrode) there is an abundance of free electrons (see Figure 4). In the semiconductor, selenium, the number of free electrons is very small. When negative-electron potential is applied to the good conductor (front electrode) and opposite polarity to the back plate, an electric field is set up across the barrier layer. Because of the fact that the barrier layer is extremely thin, a small electromotive force will produce a steep potential gradient. With this negative potential applied to the front electrode, the free electrons are accelerated to sufficient velocity to penetrate the barrier layer and the selenium layer, establishing a conduction path for the flow of current in the opposite direction, that is, from the back plate to the front electrode. To support this theory, the selenium rectifier has high resistance in the forward direction at very low voltages. The more potential applied, the more electrons are accelerated sufficiently to reach the back plate, and more current will flow through the rectifier. There is, of course, a limit to the voltage which may be applied (see Figures 5 and 6).

If the polarity is reversed, a different condition exists. Then the semiconductor (selenium layer) has to produce the free electrons. There being only a few free electrons in the selenium, only a very few will be able to penetrate the barrier layer and make contact with the front electrode. As a result, only a very small current will flow. This is called "reverse current" or "back leak." Study of a back-leak curve (Figure 5) discloses that as the reverse voltage is increased, the back leak in-

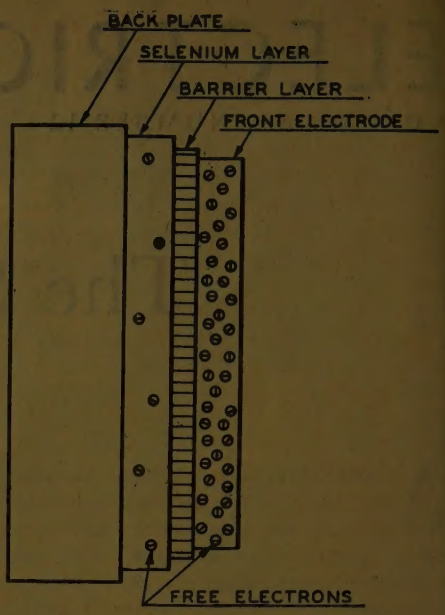


Figure 4. Proportion of free electrons in the good conductor and semiconductor layers of the selenium rectifier

creases. This may be carried to a point where rupture occurs because of the tremendous voltage gradient at a point where free electrons are present.

If this explanation of the operation be correct, then it can be assumed that thickness of the selenium layer will be a factor in the performance of the rectifier. This has been found to be the case, the forward resistance being a function of the selenium and barrier layer and the front electrode, the reverse resistance the function of the barrier layer.

The barrier layer is probably the most important part of the rectifier as far as performance and stability are concerned.

After the selenium has been applied to the back plate, the selenium surface exposed is treated specially to produce the best and most stable barrier layer. The plate then is masked at the outside edge, and the center and front electrode applied. The front electrode is a special metal alloy which in conjunction with the selenium and through subsequent treatment produces an effective barrier layer. When the plate is completed mechanically, it is formed electrically by applying direct, alternating or pulsating-direct current in the reverse direction until the reverse current reaches a predetermined value. Besides forming a stable barrier layer, this process cleans the selenium of any concentration of free electrons too close to the front electrode. This cleaning process may be evident on the surface of the front electrode in the form of a tiny rupture at the point of high concentration of free electrons in the selenium. This does not affect the performance of the rectifier. The forward resistance increases slightly during the formation process.

Rectifiers left idle for long periods of time may relax, and, when tested at low

voltage, may have low reverse resistance. When full voltage is applied, the normal reverse resistance is restored at once. There is, however, enough lag to give high instantaneous currents. In practically all rectifier applications this may be disregarded. However, on sensitive valve applications it must be taken into account. Sometimes this characteristic is desirable, sometimes not, depending upon the application.

If the rectifier is abused, either by overvoltage or current overload, especially in battery-charging applications, permanent damage can be done to the barrier layer. This, of course, results in high a-c and d-c back leak. After damage of this nature, no amount of normal operation or forming process will restore the damaged barrier layer.

GRADING OF PLATES

Selenium rectifier plates are graded by forward and reverse resistance and according to the type of rectifier circuit.

Grading by forward resistance is probably the most important, as plates that are operated in parallel should be matched properly to prevent unequal current distribution. Slight variation in forward resistance can be tolerated, however, as those plates with lower resistance will balance with the other plates as the rectifier ages.

Although keeping the reverse resistance high is important, it produces no effect, except heating, in most circuits. In half-wave rectifying circuits the output voltage across the load is affected by the reverse resistance of the rectifier because of the subtracting effect of the reverse current through the load. This condition does not occur in other circuits.

Plates are so graded and assembled into elements to produce a mean value of output voltage at the plate rating within plus or minus five per cent for resistance or inductive loads.

Only plates of the same size can be connected together whether in series or parallel. To do otherwise would produce an unbalance of current in the plates which would cause the smaller plates to carry more than their normal capacity.

It is not recommended that elements be reassembled, once they have been assembled for a specific application.

PROTECTIVE COATING

In general atmospheres which will damage other electrical equipment will have similar effects on selenium rectifiers unless they are properly protected. Fortunately, all of these conditions can be overcome, even to the extent of immersing the rectifier in oil.

Probably the worst offenders against selenium rectifiers are mercury, salt, and fungi. Coatings are available to protect against these conditions. Rectifiers generally are coated before they leave the factory except when for special reasons the coating is omitted. The coatings, however,

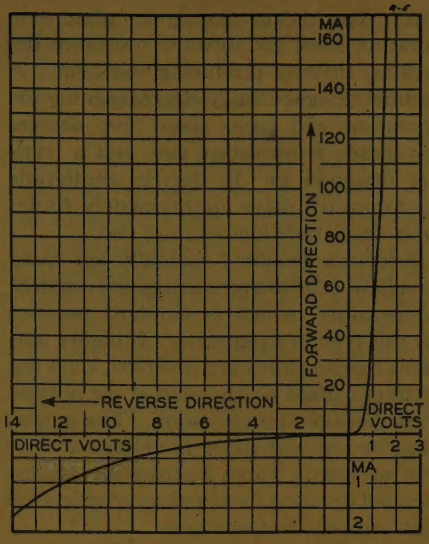


Figure 5. Static characteristic of a selenium-rectifier disk with an effective surface area of one square centimeter

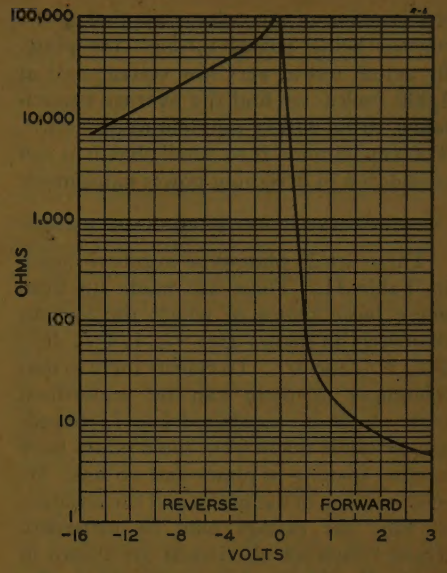


Figure 6. Static resistance characteristics of selenium rectifier disk with a surface area of one square centimeter

Test made on one-inch-diameter plates

are no better than the manner in which they are applied and handled, after the rectifiers are finished. Care must be taken not to damage the finish in handling, for once the finish has been scratched through, the rectifier is vulnerable to attack.

Users of rectifiers should not apply ordinary lacquers or other surface finishes to an unfinished rectifier, or to one that has been coated, without consulting the supplier. It is possible that the finish may be destructive, if applied directly to the plates or in combination with the finish already on the plates.

EFFICIENCY

The selenium rectifier has the advantage of high efficiency which is maintained over a wide range of output. Efficiencies range from 65 to 85 per cent, depending on the type of circuit and load (see Figure 7). Because of the nonlinear resistance characteristics, the efficiency remains practically constant from 20 per cent to 140 per cent load. Best efficiency is obtained by operating the rectifier as near full voltage as is practical. The losses in the rectifier are a function of current and are the same for all values of alternating input voltage.

REGULATION

Because of the nonlinear resistance characteristics of the selenium rectifier, most of the output voltage regulation occurs between zero and 20 per cent load. Therefore it is desirable, if regulation is important, to operate the rectifier between 20 and 100 per cent load.

In most applications this can be taken care of by selecting the proper capacity rectifier, so that the minimum load on the rectifier will be between 15 and 20 per cent. The actual *no-load* condition is never of any importance, because at this condition the output of the rectifier will not be connected to any load. If conditions are such that the minimum load is below 15

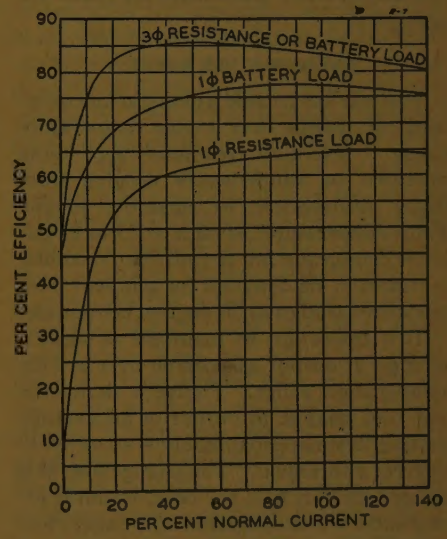


Figure 7. Efficiency of selenium rectifier at full voltage per plate

per cent and close regulation is necessary, then a bleeder load or other means of control must be used.

The fact that over half the regulation occurs in the first 15 to 20 per cent of load makes possible good regulation over 80 per cent of the load range.

Regulation of the rectifier can be calculated from the voltage-drop curves in Figure 14, by selecting the voltage drop at the minimum and maximum load condition and solving for the direct voltage. Regulation on various sizes of plates and loading is between 8 and 17 per cent from zero to 100 per cent load.

CAPACITANCE

Although selenium rectifier plates have capacitance, under practically all conditions the small capacitance present can be neglected. The capacitance per unit area

is a function of the voltage applied or the current being passed through the plate. By actual test at very low voltage and at 1,000 cycles, we find the average capacitance to be 0.0155 microfarad per $(cm)^2$. The capacitance is so small that it is not considered in designing power equipment.

VOLTAGE AND CURRENT CHARACTERISTICS

There are 21 plate-type numbers shown in Table II. These are made up from seven basic plates of which the various diameters in inches are: 3/4, 1, 1 3/8, 1 3/4, 2 5/8, 3 3/8, and 4 3/8. To classify them so that references to them can be understood basic plates are numbered 1 to 7. Standare elements made from these plates have normal spacing of plates and no fins. By wide spacing of the plates and the addition of fins, the ratings may be increased. These values for all circuits are shown in Table II. By increasing the rating of a plate in this manner, the efficiency of the rectifier may be expected to be reduced slightly because of increased voltage drop at the higher rate. This also must be considered when allowances are made for aging.

TEMPERATURE RANGE

The war has created a demand for equipment to operate over a wide temperature range. Selenium rectifiers are being operated in temperatures ranging from frigid stratosphere to steaming jungle. Plate temperatures as high as 75 to 85 degrees centigrade are permissible, enabling rectifiers to be designed for ambient temperatures as high as 70 degrees centigrade.

It is important to maintain as small a change in output as possible over a wide temperature range. Figure 8 shows the per cent change in output of an average rectifier from approximately −40 to +130 degrees Fahrenheit. The change over this range was only 8 1/2 per cent, which is remarkable when compared with other electrical devices. Figure 9 shows the a-c and d-c leakage versus temperature. At full alternating and direct voltages, the leakage currents increase at low temperatures. This high leakage is of no concern, however, since it is reduced to normal as soon as the plate reaches normal temperature from either increased ambient or plate temperature rise from leakage current. At half voltage the d-c leakage is practically constant. All bridge rectifiers used to charge batteries will be required to block approximately one half of the plate direct-voltage rating, as in each single or three-phase bridge rectifier, the direct voltage is blocked by twice as many junctions as there are series junctions in each arm. Therefore, d-c leakage will not be affected to any extent by temperature changes in bridge-rectifier circuits. In resistance and inductive loads there is no problem of d-c leakage.

Unlike ordinary resistance devices, selenium rectifiers have negative coefficient of resistance with respect to temperature. This means that as the plate temperature rises, the forward resistance decreases, thereby producing less heat. A rectifier designed for 70-degrees centigrade operation will have about five degrees centigrade temperature rise, and a rectifier designed for 35 degrees centigrade operation will have approximately 40 degrees centigrade temperature rise.

Selenium rectifiers normally are rated on a basis of 35 degrees centigrade ambient temperature, which is standard practice for this type of equipment. If higher ambient temperatures are involved, then the rectifier cannot be operated at the normal 35 degrees centigrade rating. The output must be reduced either in voltage or current or both. Table I shows the recommended derating for selenium rectifiers for high ambient temperatures.

Since plate temperature and temperature rise above ambient temperature influence the life and performance of rectifiers, it is worth while to mention a few construction characteristics in which temperature is involved.

Close spacing of plates on the center bolt retards heat dissipation, of course. Rectifier stacks have normal ratings at normal spacing. These ratings can be, and often are, exceeded by spacing the plates at two to three times the normal spacing.

The number of plates in a single element also has an effect on the operating temperature. Stacks of few plates have less temperature rise than those with a large number of plates. The ratings of the plates listed in Table II are based on the maximum number of plates per element.

When rectifier elements are mounted in cabinets with other heat-generating apparatus, they should be arranged in such a manner so that the heat from the other apparatus does not pass over the rectifier or radiate to it. In other words, the rectifier should be mounted below the other apparatus and in such a position that the free flow of air through the element is not impeded. Stacks always should be mounted with the plates parallel to the flow of air through the cabinet. Cabinets should be so ventilated that air may enter the bottom and leave at the top or at some point above the rectifier stack. The chimney effect of this arrangement is helpful in maintaining good operating temperatures.

High-current rectifiers sometimes present loss problems which may result in heat because of eddy-current losses in the steel housing. In such cases busses and rectifier stacks carrying heavy currents should be located so that the field set up by the current does not reach the steel housing. This is especially important in oil-cooled rectifiers, since the resultant heat will raise the temperature of the oil.

Wherever possible, the rectifier should be mounted in the coolest location, and the maximum ambient likely to be encountered always should be considered.

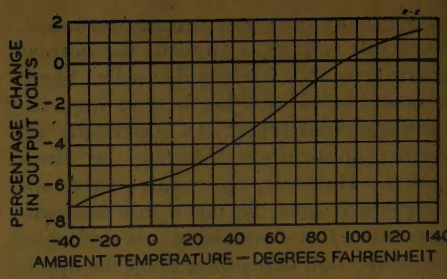


Figure 8. Change in voltage output with ambient temperature for a selenium rectifier with normal resistance load

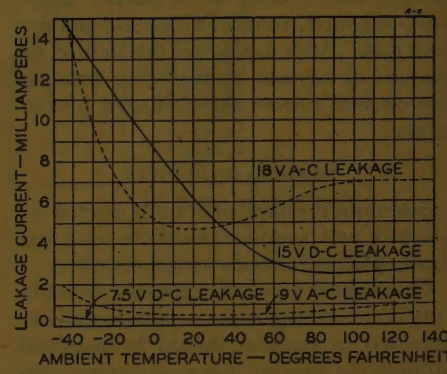


Figure 9. Effect of temperature on alternating and direct leakage current for a number-2 selenium-rectifier plate

It has been observed that the forward resistance is increased permanently, if the rectifier is subjected to temperatures below −40 degrees centigrade for extended periods of time. Temperatures below −40 degrees centigrade rarely are encountered, and no accounting is normally necessary.

For applications below −40 degrees centigrade the rectifiers must be stabilized for the minimum temperature and then regraded before the elements are assembled. This is especially true for regulator applications.

SHORT-DURATION OPERATION

There are many applications of rectifiers for intermittent service of short duration. In these applications it will not be necessary to choose a rectifier having current capacity sufficient for continuous operation. The amount of overload which can be applied naturally will depend upon the duty cycle.

Selenium rectifiers may be operated at overloads up to eight to ten times normal current, depending upon the circuit application and time allowed for the rectifier to return to normal temperature.

The current value at which a selenium plate may be operated for short periods is governed by the time required for the plate to reach its maximum temperature and the voltage drop and losses permissible in the element. When rectifiers are designed for intermittent service, the time cycle of load "on and off" periods must be taken into account, as this has a direct

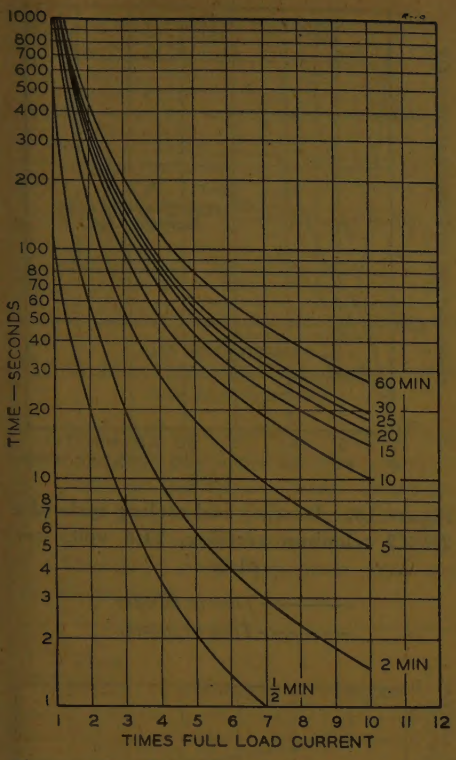


Figure 10. Operating time in seconds for a selenium rectifier at various overloads and cooling periods with ambient temperature 95 degrees Fahrenheit

Cooling time is given in minutes

bearing on the cooling and, consequently, the actual current rating.

In this service, the maximum voltage ratings of the plates cannot be exceeded, even for very short intervals. The *no-load voltage* impressed on the input a-c terminals of the element therefore governs the alternating-voltage rating of the element. Where the a-c circuit is switched with the load on, the load alternating input voltage governs. The maximum alternating voltage which may be applied to the rectifier always must be determined, and the rectifier designed for this voltage.

Normally the plates should not be operated at currents greater than five to seven times normal ratings, as the resultant voltage drop and losses in the element are high above this point.

There are so many factors to consider in this type of service that it is a little difficult to make any general rule apply. Therefore, separate consideration of each of these applications is preferred and, if necessary, actual test runs. However, a group of curves shown in Figure 10 has been prepared which will give an approximate time and overload factor providing that the off period is known. However, one may work directly from the curve. This curve also considers the maximum number of plates per stack. Figures 11, 12, and 13 show characteristics of specific rectifier combinations. The rectifiers used in Figures 11 and 12 were operated half wave, and consisted of one plate each. The rectifier used in Figure 13 was operated half wave and was made up of eight plates.

RECTIFIER-ELEMENT DESIGN

The direct output voltage of any rectifier is equal to the alternating input voltage minus the voltage drop in the rectifier, which results from current passing through the plates, divided by the form factor for the particular circuit. For explanation purposes, single-phase half-wave and single-phase-bridge rectifiers will be considered to be operating into a resistance load of one ampere.

Single-Phase Half Wave. The formula for single-phase half wave is

$$E_2 = \frac{E_1 - ND}{2.3}$$

where E_2 is direct output voltage, E_1 is alternating input voltage, N is the number of plates in series (in this case one plate is considered), and D is the voltage drop per plate at the current at which the rectifier is operating. The value of D is taken from a voltage-drop curve, which will be explained later. In this example $D=1$. The value of 2.3 is the form factor for a single-phase half-wave rectifier operating into a resistance load. The maximum alternating input voltage per plate is 18, and this value will be used. Therefore,

$$E_2 = \frac{18 - 1 \times 1}{2.3}$$

or approximately 7.4. This same formula is used for inductive loads. The direct output voltage will vary slightly depending on the characteristics of the transformer used, but generally the foregoing is correct.

To go back to the problem, in which the direct output voltage has been determined; Before selecting a transformer, the alternating current required as well as the voltage must be known. Without going into the mechanics of the a-c to d-c ratio, one notes that I_1 is 1.57 times I_2 for resistance loads or 1.57 amperes. For explanation of a-c to d-c ratios in rectifiers, see reference 8. Therefore, an a-c input of 18 volts with 1.57-amperes capacity is needed. In half-wave rectification there is a lagging power factor, because only half of the a-c wave is rectified. Therefore, wattless current to the extent of the power factor is present.

Table I. Rectifier Rating at Elevated Temperatures

Ambient Air Temperature		Recommended Current, Per Cent of Normal	Recommended Voltage, Per Cent of Normal
Degrees Centigrade	Degrees Fahrenheit		
Up to 35	95	100	100
40	104	83	100
40	104	100	80
45	113	67	100
45	113	83	80
50	122	47	100
50	122	67	80
55	131	47	80
55	131	67	60
60	140	30	80
60	140	47	60
65	149	30	60
65	149	47	40
70	159	30	40

Because of the poor power factor of half-wave rectifiers, they are not generally used on high-power units but are confined to low-capacity applications where economy or the half-wave characteristic is desired.

For battery charging the same formula is used except with a different form factor and a different value for D (see Figure 14). In battery charging and capacitive loads the transformer characteristics have more effect upon the output voltage than in resistance load. It is, therefore, generally wise to make some actual tests before final design is completed. This is not as severe an injunction as it may seem, except in cases where no voltage control over the input of the rectifier is provided. The alternating input voltage is generally controllable over a sufficient range to obtain maximum and minimum currents.

The foregoing example was worked out to determine the maximum output for one plate. With this information the number of plates required for any voltage can be determined by dividing the desired output voltage by 7.4. If 74 volts were wanted, then ten plates would be required for a new rectifier. To this must be added enough plates to take care of the aging. With ten plates 180 volts must be applied, and, if an increase of 50 per cent in forward resistance is allowed for aging, approximately 186 volts will be required to maintain 74-volt direct current. One more plate must be added to allow for this. This method may be used for every type of circuit, considering a "unit element," with one plate in each arm, as a base and is satisfactory for determining the number of series plates for all rectifier circuits operating into resistance or inductive loads. The same applies for battery charging and capacitive loads except for single-phase half wave and three-phase half wave. In these two cases the battery or capacitance is in series with the alternating voltage additively on the blocking half of the cycle causing the plate to block the direct voltage plus the alternating voltage. Therefore, sufficient plates must be used to withstand the direct voltage plus the alternating voltage in these two cases.

Single-Phase Full-Wave Bridge. If the same reasoning as was used for half wave is applied, the maximum direct output voltage from a unit bridge having one plate in each arm can be determined. As before and for simplicity, we will consider a plate having a normal basic rating of 1.0 ampere, and a voltage drop D of 1.0 volt. The formula for single-phase bridge circuit operating into a resistive or inductive load is

$$E_2 = \frac{E_1 - 2 \times N \times D}{1.15}$$ (see Figure 15).

It will be noticed that in this formula the product of the number of plates in series and the voltage drop per plate is multiplied by two instead of one for the half-wave rectifier. When the path of the direct current is traced from one side of the alternating current to the other, it is found that the current travels in the forward direction through two plates instead of one. We, therefore, will get twice the drop of one plate.

When the equation is solved for E_2, and 18 is substituted for E_1, 1 for N, and 1 for D,

$$E_2 = \frac{18 - 2 \times 1 \times 1}{1.15} \text{ or } 13.9$$

In final design the aging factor will be taken care of as before.

The a-c to d-c ratio for single-phase bridge rectifier and a resistance load as taken from Figure 15 is 1.11. Therefore, the alternating input current will be 1.11 amperes for each ampere direct current.

For battery charging or capacitive load the formula given in Figure 15 is used. When the back voltage is considered applied to the rectifier plates in the bridge rectifier, the maximum direct back voltage per plate will be the direct voltage divided by twice the number of series plates in each arm. In the case of a unit bridge it will be two. Therefore, we cannot apply excessive direct voltage to a bridge rectifier from the battery which it will charge.

The a-c to d-c ratio is always higher for battery charging and capacitive loads for all single-phase and three-phase half-wave circuits.

The form factor on three-phase circuits is the same for resistive, inductive, and capacitive loads.

In selecting a rectifier element for a certain output the direct voltage and current required must be considered. For economic reasons the largest plate generally always should be used to obtain the current required. For instance, if a single-phase bridge rectifier were wanted to deliver 12 volts direct current at 1.2 amperes to a resistance load, this would be the procedure:

First the proper plate size is selected from Table II, which in this case would be a number 5 plate, basically rated at 1.2 amperes for a bridge circuit. Next, we figure the maximum direct voltage obtainable from a unit bridge. To do this we must get the value for D from Figure 14. As this is a basic-rated plate, 1.2 amperes is normal current, so we take the value for D at 1.0 times normal current, or approximately 1.0. The constant for the number 5 plate in Figure 14 is 1.0. The value for D is therefore 1.0. By applying the formula, 13.9 volts direct current is obtained. Therefore, a unit bridge of number 5 plates will be satisfactory. Twice as many number 4 plates or four times as many number 3 plates could have been substituted. This would mean two unit bridges of number 4 plates or four unit bridges of number 3 plates. A better description of these three combinations would be: a bridge rectifier of number 5 plates having one series plate, one parallel plate; for number 4 plates one series, two parallel; and for number 3 plates one series, four parallel.

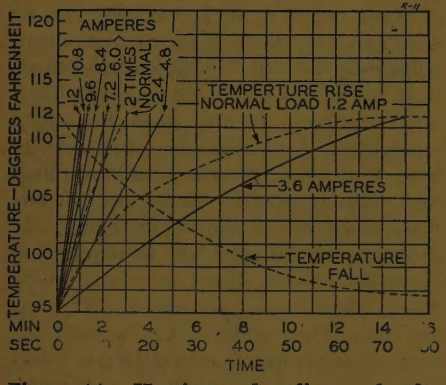


Figure 11. Heating and cooling cycle of a 6H1Al selenium rectifier operated at 18 volts alternating current from normal load to ten times normal load

Time in seconds
Time in minutes

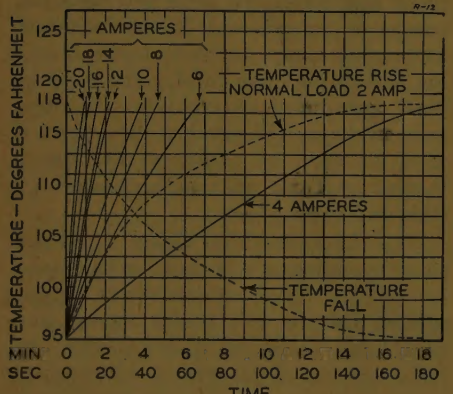


Figure 12. Heating and cooling cycle of a 7H1Al selenium rectifier when operated at 14 volts alternating current from normal load to ten times normal load

Time in seconds
Time in minutes

BATTERY CHARGING AND CAPACITIVE LOADS

In selecting rectifiers for battery charging or capacitive loads, the same general procedure is followed as for inductive or resistive loads, except that the output of the basic rating must be reduced 20 per cent for single-phase rectifiers. This may be accomplished by derating voltage or current or both. In general, it is preferred to derate voltage. No derating is necessary for battery charging or capacitive loads, when three-phase rectifier circuits are used. The same form factor is used in calculating a-c input for all types of loads in three-phase circuits.

Battery chargers having choke input filters may be calculated on basis of inductive or resistive load as far as plate rating is concerned.

In Table III are listed some of the fundamental characteristics of rectifiers for the different circuits for resistive and inductive loading.

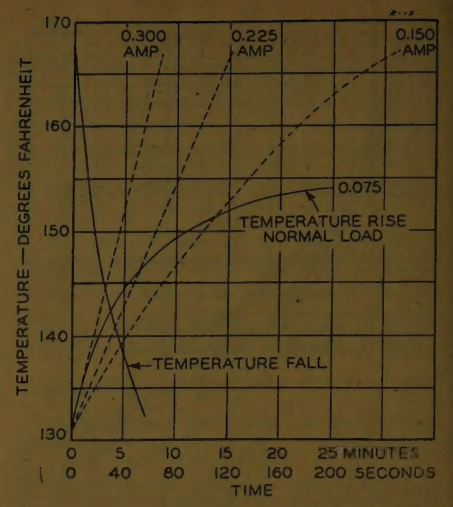


Figure 13. Heating and cooling cycle of a 2H8Cl selenium rectifier, 14.6 volts per plate

Time in minutes
Time in seconds

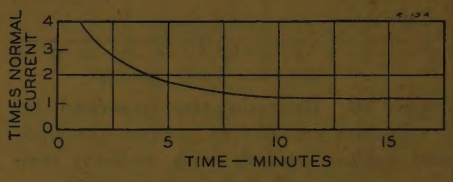


Figure 13A. Time of 2H8Cl rectifier operation to reach 167 degrees Fahrenheit with 131 degrees Fahrenheit ambient temperature at various loads

FORCED COOLING

Selenium rectifiers are normally (basic) rated at approximately 0.160 amperes per square inch at 35 degrees centigrade ambient temperature. By wide spacing and the use of fins the current density is increased to approximately 0.400 amperes per square inch. Higher current densities sometimes are used, but they require special consideration. The density is determined by the nature of the application with respect to temperature, duty cycle, and load characteristics.

Fan cooling may be employed to maintain safe rectifier-plate temperature on rectifiers that are operated at currents above their normal rating, or for protection in high ambient temperatures. The amount of air required to cool the rectifier can be calculated easily, and for all practical purposes 90 to 100 linear feet per minute is sufficient for two times normal rating, and 160 to 180 feet per minute for three times normal rating. The air actually must travel past the rectifier at these rates, if it is to carry off sufficient heat. It is well to remember the effect of obstructions in the path of the air stream upon the air velocity of a fan system which is rated in free air. The path of the air stream should be as free as possible, except for the

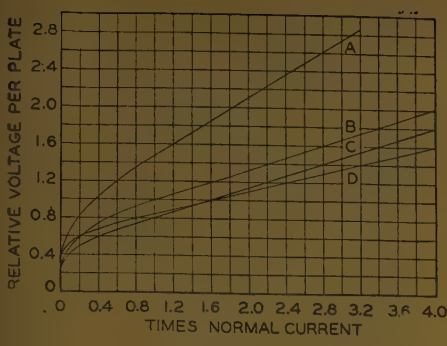


Figure 14. Approximate voltage drop in forward direction

A—Battery and capacitive load: one-phase half wave, one-phase bridge, and one-phase center tap
B—Resistance and inductive load: three-phase half wave, one-phase half wave, one-phase bridge, and one-phase center tap
C—Three-phase bridge, three-phase center tap
D—Direct current

Plate Number	Diameter	Constant
1	3/4	1.1
2	1	1.1
3	1 3/16	0.95
4	1 3/4	1.0
5	2 5/8	1.0
6	3 3/8	1.25
7	4 3/8	1.30

rectifier stacks which are being cooled. We recommend 200 to 250 feet per minute under all conditions, which should provide an ample margin of safety.

Selenium rectifiers may be immersed in oil for operating at increased loading or for operation in destructive atmospheres. Oil-cooling systems are considerably more expensive than fans and are, therefore, not generally considered. Our oil-immersed rectifiers so far have been confined to applications where the rectifiers have had to be protected against very corrosive atmospheres.

For applications where heavy overloads are experienced for short durations, the thermal capacity of an oil-cooled unit affords much more protection to the rectifier than can be obtained by fan cooling.

RECTIFIER AGING

Selenium rectifiers have the most stable output over extended periods of time, when compared with other types of dry-plate or metallic rectifiers. At normal operation the change in output is so small that in many cases no compensation for aging is necessary. This makes possible the elimination of a special transformer on those applications, and the rectifier may be operated directly from the a-c line.

The output of a selenium rectifier will decrease from five to ten per cent, when operated normally and at its basic 35 degrees centigrade rating. Naturally, if the same rectifier is overloaded, the percentage of loss in output will increase. The time required to produce this aging effect is a function of temperature, loading, circuit, and duty. No appreciable aging has been observed in idle rectifiers. Rectifiers not operated for long periods of time may show a slightly lower initial output, but this comes back to normal in a short time. In general, we have found that rectifiers operated at normal rating will reach maximum aging within 20,000 hours of continuous service. The characteristic may and does vary from one to another application. Some rectifiers will show no aging for 2,000 or 3,000 hours, whereas others show a steady aging rate. The fact that current through the rectifier is required to stabilize it may explain the difference in time required to produce stability. Apparently a sufficient number of ampere-hours must pass through the rectifier to produce final stabilized output, which accounts for the longer aging time on intermittent duty applications.

An example will explain best how aging occurs. In this aging or stabilization process, which amounts to approximately six per cent in output, the forward resistance increases approximately 50 per cent. Consider a single-phase bridge rectifier with one plate in each arm. If the normal-rating voltage drop is 1.0 volt and the single-phase bridge formula

$$E_{DC}=\frac{E_{AC}-2\times N\times D}{1.15}$$

is used, where E_{AC} = alternating input volts, N = number of series junctions in each arm, and D = voltage drop at the particular current density. E_{DC}, therefore, = 13.9. After aging, D will equal 1.5 and substituting again E_{DC} = 13.05. The aging in this case is approximately six per cent.

If the same rectifier is operated at three times its normal rating, as could be done in force-cooled rectifiers, D becomes 1.68 volts for a new rectifier. E_{DC} = 12.75. After aging, $D = 1.5\times 1.68$ or 2.52; E_{DC} = 11.25. The output drops 11.5 per cent. This same rectifier, if operated at 100 per cent rating, still will show a six per cent loss in output.

Therefore, it sometimes may be necessary to consider the aging factor in the application of rectifier stacks and to allow enough for the aging which may be expected, depending on the loading of the rectifier plates.

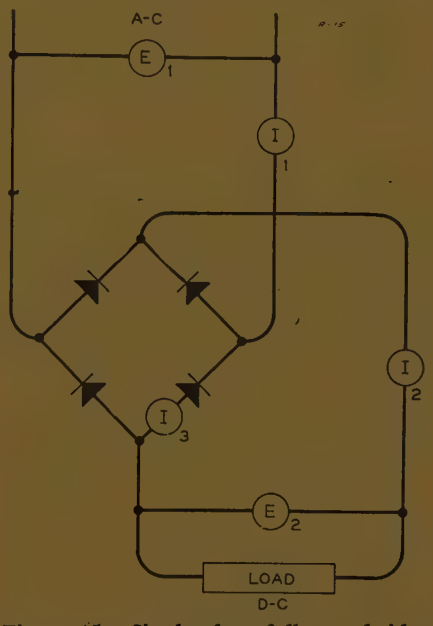


Figure 15. Single-phase full-wave bridge rectifier

Voltage and current values:
E_1—Alternating input voltage
E_2—Direct output voltage
I_1—A-c input amperes
I_2—D-c output amperes
I_3—D-c amperes each arm of rectifier circuit

Voltage values:

Resistance and inductance load, $E_1=(E_2\times 1.15)+2ND$

Resistance and inductance load, $\frac{E_2=E_1-2ND}{1.15}$

Current values:

Resistance and Inductance Load	Rms Output Current
I_2 I_1 I_3	
1..1.1..0.5	1.11

Rectifier application—D-c output, 12 volts, 1.2 amperes, load, resistance
Solution—Use 5B1A1 stack; $N=1.0$; $D=1.0$; $E_{AC}=12\times 1.15+2\times 1.0\times 1.0$; $E_{AC}=15.8$

VALVES

There are many circuits in use where valves are required to prevent the flow of current in case of a reversal of polarity. Selenium rectifiers are being applied to these applications successfully.

Each application must be considered individually, since there are inherent characteristics in the selenium plate which must be taken into account. If the application is one wherein the current flows through the selenium plate in the forward direction most of the time and only infrequently is there any back voltage applied, we must remember that at the instant of reversal of voltage, we will get high-leakage currents. This condition lasts only for a very short time, until the leakage current returns to normal. The initial leakage current may run as high as ten to 15 per cent of the normal current at full direct voltage per junction. If this is outside the limits that can be tolerated, then the back voltage per junction must be reduced by adding more plates to a point where operation is satisfactory.

Applications where alternate polarities are applied to the valve are most satisfactory.

A voltage-drop curve for d-c applications is shown in Figure 14.

The d-c ratings of all the plate sizes are

Table II. Sizes and Ratings of Selenium-Rectifier Plates

Plate Type, Number	Plate Diameter, Inches	Disk Size, Number	Fin Size, Number	Maximum D-C Amperes Continuous Output of Element With One Plate Per Arm at Ambient Temperature 95 Degrees Fahrenheit (35 Degrees Centigrade) or Less: Single Phase Half Wave	Single Phase Bridge or Center Tap	Three Phase Half Wave	Three Phase Bridge	Three Phase Center Tap	Maximum Alternating Volts Per Plate	Maximum Continuous D-C Rating as Valve 95 Degrees Fahrenheit Ambient: Amperes	Block Volts	Maximum Number Plates Per Element
1	3/4	1		0.040	0.075	0.100	0.110	0.130	18	0.06	15	36
2	1	2		0.075	0.150	0.200	0.225	0.270	18	0.12	15	36
20	1	2		0.110	0.220	0.290	0.330	0.400	18	0.17	15	28
3	1 1/8	3		0.150	0.300	0.400	0.450	0.550	18	0.23	15	36
21	1 1/8	3		0.290	0.450	0.600	0.670	0.820	18	0.34	15	28
4	1 1/4	4		0.300	0.600	0.800	0.900	1.1	18	0.45	15	40
10	1 1/4	4		0.390	0.780	1.0	1.1	1.4	18	0.58	15	28
9	2 1/8	4	5	0.580	1.1	1.5	1.7	2.1	18	0.87	15	28
5	2 1/8	5		0.60	1.2	1.6	1.8	2.2	18	0.90	15	40
11	2 1/8	5		0.78	1.6	2.1	2.3	2.8	18	1.2	15	28
12	3 1/8	5	6	0.90	1.8	2.4	2.7	3.3	18	1.4	15	28
13	4 1/8	5	7	1.1	2.2	2.9	3.3	4.0	18	1.7	15	28
6	3 1/8	6		1.2	2.4	3.2	3.6	4.5	16	1.8	12	40
14	3 1/8	6		1.5	3.1	4.1	4.6	5.8	16	2.4	12	28
15	4 1/8	6	7	1.8	3.5	4.6	5.2	6.5	16	2.7	12	28
16	4 1/8	6	7	1.9	3.8	5.0	5.6	7.0	16	2.9	12	24
7	4 1/8	7		2.0	4.0	5.3	6.0	7.5	14	3.1	12	40
18	4 1/8	7		2.6	5.2	6.9	7.8	9.7	14	4.0	12	28
17	3 1/8	6	6	2.7	5.4	7.2	8.1	10.0	16	4.1	12	28
19	4 1/8	7	6	3.7	7.4	9.8	11.0	13.3	14	5.7	12	28
8	4 1/8	7	8	5.0	10.0	13.0	15.0	18.0	14	7.5	12	28

Center bolt sizes: plates numbers 1, 2, 3, 20, 21—numbers 8–32; plates numbers 4, 9, 10—numbers 10–32; Plates numbers 5 to 19—5/16–18.

shown in Table II. Because of the absence of the alternating current in these applications the plates are rated slightly higher than for rectifier applications.

FORWARD AND REVERSE RESISTANCE CHARACTERISTICS

The forward and reverse resistance characteristics are very important in a rectifier. An ideal rectifier would be one with zero resistance in the forward direction and infinite resistance in the reverse direction.

Since selenium rectifiers do not meet these requirements, their effects on the circuits with which they are to be used must be considered. The forward resistance is not fixed or linear. At very low voltage and current the resistance is very high (see Figures 5 and 6). As the voltage and current are increased, the resistance decreases very rapidly, up to approximately 20 per cent of plate rating. From 20 to 140 per cent the forward resistance changes only a small amount. From this we would expect to have the greatest amount of regulation take place in a rectifier in the first 20 per cent of normal load. This is true, and it must be considered in rectifier applications where regulation is important. This nonlinear resistance characteristic, which at first seems to be undesirable, makes possible good rectifier regulation and suggests the possibility of their use as direct-voltage regulators.

The reverse resistance performs somewhat the same way as the forward but is not as critical. Generally, applications demand maintenance of high reverse resistance at all times. At the critical voltage in the reverse direction the resistance decreases very rapidly, and care must be taken to avoid overheating and rupture of the barrier layer at this point. Plates must not be operated at voltages above rated values.

VOLTAGE REGULATORS

In d-c applications selenium rectifiers can be operated on the steep portion of the resistance curve (Figure 6), and the rectifier plates can be used as regulators. If the plates are operated in the forward direction at about 0.70 volt per plate, the effect of a variable regulating resistance across a load can be achieved. This, coupled with the necessary voltage-dropping resistances, will provide fairly good regulation. See circuit in Figure 16.

As the voltage across the load increases, the resistance of the rectifier plates decreases rapidly, permitting more current to pass through the rectifier and creating greater voltage drop across *R*, thereby regulating voltage across the load.

Because of the very nature of the resistance characteristics, it is difficult to predetermine the exact combinations of plates for any one application. It is, therefore, desirable and most generally necessary to make actual tests for individual problems.

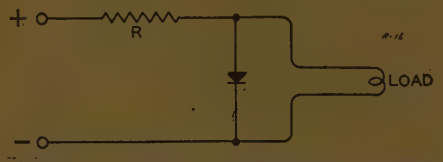


Figure 16. Selenium-rectifier regulator circuit

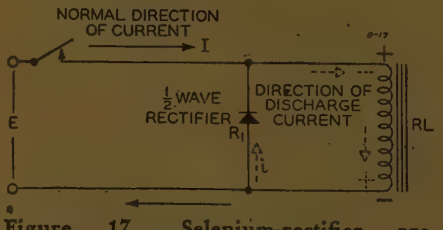


Figure 17. Selenium-rectifier arc-suppresser circuit

The curve in Figure 6 cannot be taken for all problems, since the complications which arise in each circuit are different, and one curve could not apply to all applications. Production tolerances for rectifier plates cannot be used in this type of service. In practically all applications it is necessary to select plates and match them for the particular project. The curve in Figure 6 was taken from a series of single plates.

In applying the selenium rectifier to these regulator circuits, the amount of current that will flow through the plates at maximum-voltage condition must be considered. If this current is too high for the size of plate, additional series plates must be added. It may not be possible to increase the surface area without increasing the number of series junctions, as this might unbalance the regulation circuit because of the increase in shunt current through the rectifier.

CIRCUIT ELEMENTS

The nonlinear resistance characteristic is also desirable in network circuits where small voltage changes cause great resistance changes. Selenium rectifiers are working successfully in such applications. These are difficult problems, and particular care must be used to insure proper operation. By special stabilizing of the plate and matching of it to the circuit, balanced operation usually is assured. If rectifiers that are not stabilized and matched are left idle, they may become unbalanced. In rectifier applications this unbalance may occur during long idle periods. Balance is restored, however, as soon as full voltage and load are applied and normal operation takes place without any unbalance indication. In circuit networks, however, full voltage and current generally are not applied, and the recti-

fier would remain unbalanced. Hence, stabilizing and matching plates for this type of service are important.

ARC SUPPRESSION

Half-wave rectifiers may be used across inductive circuits carrying direct current to suppress arcing across contacts and to limit voltage surges. The rectifier is connected across the inductance, matching the polarity of the power supply as shown in Figure 17. Thus, the rectifier offers a high resistance to the applied voltage and a low resistance to the surge current generated by the collapse of the field in the inductive load.

The electromotive force generated in the coil at the instant the applied voltage source is broken is

$$e_b = E\frac{R+R_1}{R} \qquad (1)^7$$

The current, i, generated in the coil at any time, t, is

$$i = \frac{I}{e^{\frac{(R+R_1)t}{L}}} \qquad (2)^7$$

e_b = voltage generated with collapse of field
E = applied voltage
R = resistance of coil
R_1 = forward resistance of rectifier
i = induced current flowing in forward direction through R_1 at time t
T = time in seconds
L = inductance in henrys
e = 2.718

In formula (1) the induced voltage varies in the ratio $\frac{R+R_1}{R}$. Then, if R_1 approaches infinity, when the switch is opened, e_b rises and high voltages are generated. This condition exists when the contact points are opened to break the current. The air gap between the opening contacts becomes ionized and arcing occurs. This arcing, from the discharge of an inductive load, will shorten considerably the life of contacts, and the application of the proper selenium rectifier will reduce this condition to a minimum.

From formula (2) it can be seen that the current i at any time t varies with the resistance R_1 of the external load. The maximum possible current which can be induced by the coil will occur when $R_1 = 0$, and $t = 0$. By substitution in formula (2) it will be found that i is then equal to I, the steady-rate direct current.

In choosing a rectifier to serve as an arc quencher there are two conditions which must be met. First the rectifier must be able to block safely the steady direct voltage; second, it must be able to carry the current without overheating. The largest current a spark-quenching rectifier will be required to carry is the steady-state direct current in the circuit. This condition will be found where the number of discharges per second is high. When the rate of discharge is decreased, the size of the rectifier may be decreased. It should be remembered that the ambient temperature must be taken into account in this application, as well as for all other applications of selenium rectifiers.

SURGE VOLTAGES AND CURRENTS

Although excessive voltage or current are always undesirable and often cause damage to a dry-plate rectifier, selenium rectifiers will withstand high overvoltage and current for short duration.

For durations of 20 to 30 microseconds, tests show that back voltages as high as 140 to 250 volts may be applied without breakdown. This means that the maximum safe voltage would be somewhere below 140 volts. These tests do not mean necessarily that we should design for such overvoltages. This ability to stand overvoltage is helpful only in case of emergency voltage surges such as may be caused by lightning discharges.

Surge currents in the forward direction may exceed the normal rating by 30 to 50 times for durations of 20 to 30 microseconds under good contact conditions between front electrode and the contactor plate.

REFERENCES

1. Selenium Rectifier for Signaling, **J. E. Yarmack, C. G. Howard.** *Railway Signaling*, volume 32, December 1939.

2. Selenium Rectifier Characteristics, Application and Design Factors, **C. A. Clarke.** *Electrical Communications*, volume 20, 1941, number 1.

3. Selenium Rectifiers and Their Design, **J. E. Yarmack.** *Electrical Communications*, volume 20, 1941, number 4.

4. The Characteristics and Applications of the Selenium Rectifier, **E. A. Richards.** *Journal* of The Institution of Electrical Engineers (London, England), volume 88, October 1941, part 2.

5. Characteristics and Applications of the Selenium-Rectifier Cells, **E. A. Harty,** AIEE *Transactions*, volume 62, 1943, October section, pages 624–9.

6. United States patent 2,314,104. **E. A. Richards, L. J. Ellison,** International Standard Electric Company.

7. Electrical Engineering (book), **C. Christie.** McGraw-Hill Book Company, Inc., New York, N. Y. Pages 108–11.

8. Theory and Performance of Rectifiers, **H. D. Holler, J. P. Schrodt.** Department of Commerce Bureau of Standards. Pages 472, 484.

The Centenary of the Morse Telegraph

H. W. BIBBER
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THE MAGNITUDE of the telegraph industry, its effect on daily life, the stimulus it has given to scientific enquiry and electrical engineering, and especially the influence it had on the formation of the AIEE, all combine to make it fitting that our Institute do Morse and his associates honor on the 100th anniversary of their first public telegraph line between Baltimore, Md., and Washington, D. C. A session in recognition of the occasion was held during the 1944 AIEE summer technical meeting.[1–3]

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To some of our younger members, it may be news that of the 25 men who signed the original organization "call" for the AIEE published in the April 15, 1884, issue of *The Operator* (the leading American electrical journal of that time), 21 were in the telegraph business. The first president of the Institute was Norvin Green, president of the Western Union Telegraph Company. Two of the initial vice-presidents and four of the initial managers were in the telegraph industry. In succeeding years telegraph men continued to exercise influence in shaping the destiny of the Institute, and today they form a valued group of our membership.

In keeping this centenary we do not commemorate the first working telegraph in the world, as it appears that a line 13 miles in length from Paddington Station to West Drayton on the Great Western Railway in England was put into operation in 1838. This was constructed on Professor Charles Wheatstone's scheme employing six wires and five needles, wherein two converging needles were made to point to letters on a dial. We do not observe the anniversary of the invention of audible electromagnetic signaling at a distance, for Joseph Henry demonstrated to his students at the Albany Academy, Albany, N. Y., in 1832, the ringing of a bell by electromagnetic action through a circuit of a mile of wire. Nor do we celebrate the passage of a century since Morse first demonstrated publicly the

practicability of his invention. Successful operation was shown by Morse (through ten miles of wire) in New York City at New York University in January 1838, in Philadelphia before a committee of the Franklin Institute the same month, and in Washington in February of that year before the House committee of commerce.

Rather do we celebrate the persistence of Morse and his associates in overcoming the obstacles they found in their path due both to Nature and to man. From the year 1844 the development of the telegraph industry in the United States was spectacular. Through the extension of telegraph lines to all parts of the country there came to be aroused an interest in this new force or power, electricity, and many electrical inventors in other fields began in telegraphy.

The transmission of a telegraph message between Baltimore and Washington in 1844 introduced a new era in communication and in the practical application of electricity and perhaps led indirectly to the founding of the AIEE. The early struggles and discouragements of the artist-turned-inventor Samuel Finley Breese Morse, at last crowned by success, are an exemplary chapter in American life.

EARLY DEVELOPMENTS IN TELEGRAPHY

It is not appropriate in this paper to review in detail the experiments previous to Morse's in which electricity had been tried as a means of communicating intelligence. These attempts were very numerous; indeed a volume of several hundred pages by J. J. Fahie is entitled "A History of Electric Telegraphy to the Year 1837." Suffice to say that frictional electricity had been used for signaling over a distance of some thousands of feet nearly 100 years before 1844, and that experiments employing dynamic electricity with chemical receiving indicators had started soon after Volta's invention of the "pile" in 1800. Oersted's discovery in 1820 of the effect of the electric current in a wire on a neighboring compass needle led to the development of needle telegraphs by several European inventors in the following years.

This brings us to Samuel Finley Breese Morse. What sort of man was he and how did he become interested in the electromagnetic telegraph?

Born in Charlestown, Mass., in 1791, the son of a militant orthodox clergyman who was one of the distinguished promoters of a knowledge of geography, he was raised in an intellectual atmosphere that must have been stimulating to say the least. By the time he was 14 he had completed the preparatory studies at Phillips Andover Academy for Yale College and entered there. His self-taught skill in art developed to the point where during his senior year he was able to charge five dollars for miniatures painted on ivory. Indeed, as his college work came to a close, his interest in art exceeded his interest in any academic studies, even in natural philosophy, where he was first introduced to electricity by Professor Silliman.

He persuaded his father to send him to England in 1811 to study art, which he did with seriousness of purpose and success. As he left England to return to the United States in 1815, he felt that his real interest lay in painting historical scenes which he felt required more imagination and were more intellectual than portraits. Ironically enough, when he started out to earn his living as a painter in New England and soon afterward in South Carolina, he found that he could get commissions for nothing but portraits, and these in none too great quantity. In the intervals between portrait commissions he turned to other interests, showing his extraordinary versatility. He and his brother were the patentees of a water pump, and he worked diligently for a time on a marble-cutting machine to make easier the carving of statues of this stone. On going to live in New York he helped to found and became president of the National Academy of the Arts of Design. He attended Professor Dana's lectures on electromagnetism given at Columbia College. In his early years in New York he helped found the Journal of Commerce, a paper that has had a long career. He was often in straitened financial circumstances.

With the promise of sufficient financial backing to make possible his return to England, and a tour of Europe in the interests of art, he left New York in November 1829 and spent the next three years on the continent. While in France he noted in his letters the use of the Chappe semaphore telegraph devised during the French Revolution, and commented on its advantages and limitations.

It was on his return from Europe in 1832 on board the sailing ship *Sully* that the idea of an electromagnetic telegraph seems to have occurred to him. Morse later described the circumstances as he remembered them. At the lunch table about the middle of the trip, Doctor C. T. Jackson of Boston who was as much interested in "natural philosophy" as in medicine, was discussing the recent experiments by Ampère with Sturgeon's electromagnet. On being asked whether the flow of electricity was not retarded by the length of the wire, he replied that electricity appeared to pass instantly over several miles of wire. Morse then said, "If this be so, and the presence of electricity can be made visible in any desired part of the circuit, I see no reason why intelligence might not be instantaneously transmitted by electricity to any distance." Morse then left the group and pondered ways in which a practical instrument could be constructed, making sketches of his thoughts in a notebook in which he recorded sketches and ideas for paintings.

His first idea of sending intelligence was to use an electromagnetically actuated stylus to mark a moving strip of paper with dots and dashes appropriately spaced. The dots and dashes were to represent numbers, which would be used to identify numbered words in a dictionary. The proper arrangement of dots, dashes, and spaces between was to be secured by setting type bars with appropriate faces of dots and dashes in a "port rule" and running these at constant speed under a button attached to a key in such a way that the circuit was opened and closed to correspond to the projections and depressions of the line of type.

On his return to New York he found the same financial difficulties which had dogged his steps before, and he returned to painting as a livelihood and to politics as an avocation. He was appointed professor of sculpture and painting in the newly formed University of the City of New York in 1832, but he received no salary except fees from his students and was not required to give any lectures. In 1835 he was given a new title in New York University, professor of the literature of the arts of design; assigned some regular lectures; and granted the free use of a large room in the then new Washington Square building.

In the fall of that year he showed a working model of his telegraph to friends, and in January 1836 a professor of science at the university, Leonard D. Gale, visited Morse's rooms and saw his telegraph. This was the original model in which a pencil was moved transversely on a continuously moving paper tape. Painting took most of his time, however, and it was not until 1837 that the report of a new French telegraph stimulated him to renewed effort on his own. He realized that he needed scientific help, and enlisted Professor Gale as a partner in the enterprise. The latter's immediate contribution came from his knowledge of a paper of Joseph Henry's dealing with improvements in electromagnets. Gale suggested that instead of a few turns of wire wrapped around each leg of a horseshoe receiving magnet fed by a "quantity" battery, Morse had better employ a large number of turns and use an "intensity" battery. The improvement in operation was marked. Where difficulty had been experienced in sending messages through a hundred feet of wire, it became possible by November 1837 to operate the instrument through ten miles of wire.

The telegraph relay, an essential element of the Morse system, was already in Morse's mind in 1836, according to Gale's later testimony, and a definite workable design was shown to Gale in 1837. It

appears that Joseph Henry invented the relay earlier, but as it had not been described in anything Morse had read, nor did Morse make Henry's acquaintance until late in 1837, it was evidently an independent invention by Morse. He originally conceived of its use as a one-way repeater, believing that electromagnetic action could not take place over too many miles without being "renewed" by the action of another battery.

IMPROVEMENT OF THE TELEGRAPH

The House of Representatives in February 1837 had passed a resolution directing the Secretary of the Treasury to investigate the feasibility of setting up a "system of telegraphs for the United States." In March 1837 a circular of inquiry was issued by the Secretary, and Morse was further stimulated to every effort to improve his invention. Thus it was that he was ready to welcome the efforts of Alfred Vail who offered mechanical skill that Morse did not possess, and financial support for the enterprise through his brother and father.

Young Vail had graduated at New York University in 1836 intending to go on with study of theology in preparation for the Presbyterian ministry, but the chance visit to Professor Morse which he made in the early autumn of 1837 changed the course of his career. His father was the fairly prosperous proprietor of the Speedwell Iron Works in New Jersey, where Alfred had received an apprenticeship in the mechanic arts. Alfred felt certain that he could persuade his father and brother to support the telegraph enterprise in his name. It seemed to him that here was an undertaking into which he could put his heart. He and Morse then entered into a contract on September 23, 1837, whereby Vail undertook to construct at his own expense one of the telegraphs of Morse's design to be exhibited before Congress, and to pay the costs of the exhibition and United States patents. In return Vail was to receive one-fourth of all rights in the invention in the United States, and a share in any foreign patents which he might furnish the means to obtain.

Morse completed a caveat for his patent on October 3, 1837. In it he claimed:

1. A system of signs by which numbers are signified.

2. A set of type adapted to communicate the signs.

3. An apparatus for regulating the movement of the type so that the electric circuit may be made and broken in accordance with the moving type.

4. A register for recording the signs at the receiving end.

5. A dictionary or vocabulary with each word numbered.

6. Modes of laying the conductors to preserve them from injury.

Looking back on the work of Morse, his predecessors, and his contemporaries, it can be asserted that his was the first *recording* electromagnetic telegraph. The elements of his device were all known at the time. He made no new basic discovery, but he made a new combination of the known elements.

It is to Vail that credit appears to be due for changing the relative position of the magnet and armature so that a vertical motion of the marking stylus was achieved, making possible the registration of real dots and dashes.

With the improved recorder and a new battery built by Professor Gale, the telegraph was exhibited publicly to the people of Morristown, N. J., on January 10 and 11, 1838, with operation over three miles of wire. On January 23 it was shown to hundreds of people in New York with the new alphabetic code which did away with the numbered dictionary, then to a committee of the Franklin Institute at Philadelphia, and at last shown in the committee room of the House committee on commerce to that group itself and to large numbers of interested Congressmen. The demonstration was a complete success and the chairman of the committee was instructed to report an appropriation bill for $30,000 to construct an experimental line from Baltimore to Washington.

In the course of the demonstration to the committee, its chairman, F. O. J. Smith of Maine, had become enthusiastic about its money-making possibilities as a private enterprise and wanted to become a partner. The existing partners, Morse, Vail, and Gale, felt the need of an aggressive assistant in promoting the telegraph on the political and economic side, and so Smith was taken in. This event was the occasion for a formal statement among the partners as to their relative shares of ownership: Morse, 9; Smith, 4; Vail, 2; Gale, 1. Vail and Morse had reduced their interest in order to take Smith in, to get the value of his legal services and aggressive disposition.



Sam'l F B Morse

Morse and Smith left for Europe in the spring of 1838 with the hope of obtaining foreign patents on Morse's invention. Disappointment came in England where the Attorney General ruled that previous publication debarred Morse from obtaining a patent, in France where the dilatory procedures of government wore him out, and in Russia where the Czar finally decided that he did not want telegraphic communication after all. The bill reported to Congress from Smith's commerce committee did not reach a second reading, so hard was it to get the members of the House to realize that this was more than a fascinating toy.

Morse returned discouraged in 1839 and passed through a very troubled period. His patent was issued on June 20, 1840. But the financial depression that started in 1837 had enveloped the whole country and even the Vails had no further funds to expend in promoting the telegraph. Smith had experienced financial reverses. Morse now came into prominence and secured needed finances as a daguerreotypist, even teaching the art to others for a fee. Though he practiced steadily for little more than two years, he earned considerable fame.

In October 1842 he laid a wire coated with pitch from New York's Battery to Governor's Island and until it was caught by a boat's anchor it provided successful communication under water. It was not the first submarine telegraph in the world, but the first in America.

BUILDING OF THE FIRST LINES

During the autumn he again memorialized Congress and, at the risk of becoming penniless, put on a more striking demonstration than ever between two rooms of the Capitol. A new appropriation bill of $30,000 was reported by the commerce committee, but compared to other legislation it lacked priority. The end of the session March 3, 1843, approached. The telegraph-appropriation bill passed the House on February 21 and it seemed as if the jammed Senate calendar could not possibly permit of its passage. But after Morse had given up hope on March 3, so the legend goes, as evening approached, and he had returned deeply discouraged to his hotel, his bill was passed and signed by President Tyler. The young lady who called at his hotel the next morning to inform him of the great news was Annie Ellsworth, daughter of the Commissioner of Patents. To her, a little more than a year later, Morse gave the privilege of composing the first message to be sent over the completed line.

With the Congressional appropriation granted, Morse proceeded to plan on laying the lines underground, insulated in lead pipe two feet below the surface. As the available money would not permit

purchasing land for a right of way, application was made to the Baltimore and Ohio Railway for permission to bury the telegraph wires along its line between Baltimore and Washington. The granting of permission constituted the start of a cooperation between the railway and the telegraph that has existed ever since. A contract was let for furnishing the lead pipe with insulated wires inside.

The trenching contract was placed with F. O. J. Smith for disposition. Into his office in Portland, Me., there came one day in 1843 a young plow salesman, Ezra Cornell, of Ithaca, N. Y., who was later to found the university which bears his name. At Smith's request, Cornell invented a combined plow, cable-reel, and filler-in drawn by oxen which successfully laid the lead-pipe cable at a rapid rate compared to hand trench digging. The cable was being laid faster than the maker could supply it. Then, after nine miles were in the ground, tests showed that the insulation had failed. The newspapers were following the enterprise closely, and Morse could ill afford a failure of his scheme now. He arranged with Cornell to contrive a breakage of the special plowing machine so that time could be secured for tests and experiments with other manufacturers' pipe, and other methods of insulation. The experiments were all disappointing. It was a period of deep discouragement, as much of the original $30,000 now had been spent. But Vail read in an English journal of the success which Wheatstone and Cooke had attained with wires placed overhead on poles after underground lead-pipe cable had failed.

Morse decided that this was the answer to the problem, and in February 1844 work was started on a pole line along the railroad right-of-way. Smith in the meantime had become a disturbing element in the enterprise, and he and Morse became life-long enemies. During the stringing of wires on the pole line tests were made frequently to make sure that all was well this time. In the course of these Alfred Vail discarded the "port rule" and type mechanism by which the code letters were sent, and found Morse could read the receiving tape just as well when he opened and closed the circuit by hand. Thus was the telegraph key invented.

The first formal message sent over the completed line composed by Miss Annie Ellsworth, "What hath God wrought," was considered prophetic by Morse who had high visions of the public service which could be rendered by the telegraph. To keep the potential power of the telegraph for the public good, Morse was anxious to have the government buy the patent and issue regulated licenses to private parties to construct lines, and as well to build lines for its own business. But Congress was now preoccupied with the Mexican troubles, and so it was left for private enterprise to develop the new mode of communication, as soon happened.

Of the original group of partners, Morse, Vail, and Smith continued active in the telegraph business. Cornell, who had come into the enterprise at the building of the Washington–Baltimore line, also followed along in the industry. He played a large part in the founding of the Western Union Company; he was a director for 15 years, and the largest stockholder for 20 years.

SIGNIFICANTS OF THE TELEGRAPH

In retrospect, what lessons can be drawn from the work of Morse and his associates? The most evident is that indomitable persistence is necessary to convince men of the possible utility of new technical devices, and that what looks like the most serious difficulty of a series offered by nature may be the last that has to be overcome. Co-operative effort was what made the telegraph a technical success, and had Smith been of different character, co-operation would have hastened its acceptance by Congress and the country. The pure scientists, Joseph Henry in particular, played a significant part. The prompt publication of scientific discoveries and technical improvements aids material progress; Henry's paper on the electromagnet and the report of Wheatstone's use of overhead lines are striking examples.

It is interesting to note that three features which Morse tried out on the Baltimore–Washington line but which were later discarded, have returned to use in recent years:

1. A moving paper tape on which the message is recorded.
2. Mechanical rather than manual sending of the code impulses.
3. Laying telephone and telegraph lines underground by means of a plow similar to that devised by Ezra Cornell.

Turning now to the material development of the telegraph industry in the United States, what does the passage of 100 years show? In 1844 there were two public telegraph stations and a single line connecting them. In 1944 there were 33,000 public message offices; in addition there were agency stations by which 90,000 places could be reached. Used in public message and related services were 2,300,000 wire miles.

There seems to be no record available of the number of public messages sent from May 24, 1844, to the end of that year, but in the year previous to January 1, 1944, some 206,000,000 public messages were handled. Yet the preceding figures do not show completely the development of telegraphy in the United States because of the large number of private-line stations, 20,000 at the beginning of 1944, which were served by 1,750,000 circuit miles of line. Many more installations that are telegraphic in nature are represented by the 16,000 teletypewriter (*TWX*) installations and the 2,000,000 circuit miles which provide for their interconnection. The magnitude of the traffic on this *TWX* system may be judged from the fact that in the year 1943 more than 14,500,000 service connections were made, on each of which several messages may have passed.

After observing that "It was perhaps no accident that the telegraph in its most practical and successful form was invented, not by a physicist who had spent years in the study of electricity, but by an artist," R. T. Barrett goes on to say, "The genius of the great artist consists less in creating things than in combining familiar things in new relations. Fundamentally he is guided by rules with which all painters are familiar. When he surpasses his fellows, he does so in the skill with which he seizes upon commonplace things and unites them into creations of consummate beauty. It may have been the artist in Morse that led him to see, in what other men had produced in the field of electrical research, the separate elements of the telegraph; that enabled him to visualize them in new relationships."

In the life and work of Morse we can identify several features of the "American spirit" characteristic of our 19th century, which has been described so clearly by Dixon Ryan Fox in an address before the Newcomen Society. Morse showed in but few instances anything but a self-disciplined decency and concern for truth. He had a basic respect for hard work, and as a speculator in both art and telegraphy he was prepared to weight the balance with his sweat in the same way as the pioneer farmers of the West.

He was a combination of inventor and promoter, of whom the United States was fortunate in having several in the last century. Typical of many of his time he seems to have had a personal philosophy that was a blend of idealism and materialism, with the latter strain tending toward dominance. He is an outstanding example of the opportunity which America, in contrast to Europe, offered to a man to rise, fall, rise, fail, and rise again. His ultimate success showed that this was a land of opportunity, not only for young men—and let us remember that Morse was 53 years old in 1844—but for older men as well.

REFERENCES

1. American Telegraphy After 100 Years, **F. E. d'Humy, P. J. Howe.** AIEE *Transactions*, volume 63, 1944, pages 1014–32.
2. Telegraphy in the Bell System, **J. A. Duncan, R. D. Parker, R. E. Pierce.** AIEE *Transactions*, volume 63, 1944, pages 1032–44.
3. Developments in the Field of Cable and Radiotelegraph Communications, **Haraden Pratt, John K. Roosevelt.** AIEE *Transactions*, volume 63, 1944, pages 1044–7.
4. The American Leonardo (book), **Carleton Mabee.** Alfred Knopf, New York, N. Y., 1943.
5. Samuel F. B. Morse—Letters and Journals, two volumes, **E. L. Morse.** Houghton Mifflin Company, Boston, Mass., 1914.

Electrically Heated Homes in the Tennessee Valley

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THE USE of electricity for house heating is not new. As early as 1914, homes in the Northwest were being heated by electricity. It is estimated that by 1920 there were over 2,000 homes in Tacoma, Wash., using this means of heat. For a few years prior to the war, the use of electric heating spread rather rapidly on the west coast, particularly in central California.

The use of electricity as the main source of heat in homes located in the Tennessee Valley area began about 1935. It is estimated that today there are approximately 1,000 electrically heated homes in the valley. These are scattered quite widely throughout Tennessee and the northern parts of Alabama and Mississippi.

Public acceptance of this type of heating is now quite well established in this section. The experiences of those using electric heating have proved its many advantages, among which are cleanliness, elimination of fuel storage and waste removal, quick availability for sudden cool spells or to drive off the chill of an early fall or late spring evening. This form of heat offers an almost ideal means of maintaining heating comfort in the home. During the two years before the war curtailed general home building, there were quite a few new homes provided with electric-heating installations. Recent inquiries indicate that there exists a keen interest in heating homes electrically. At least four investment builders have expressed their intention of building several new "all-electric" homes, including electric heating, as soon as materials again become available.

INTEREST IN ELECTRIC HOUSE HEATING GROWING

In the Tennessee Valley region electricity is distributed to the consumer through municipalities and rural electric co-operatives which in turn purchase it from the Tennessee Valley Authority. The areas served by these systems vary widely in extent and number of customers; therefore, the problems of house-heating loads are not the same for all of them.

The growing interest in electric house heating has stimulated much discussion concerning the effect of this type of load on distribution systems. A number of pertinent questions has been raised regarding the amount of energy required and the cost of heating homes of different sizes and types of construction, the load demands of a heating installation, and the time of day when these occur.

Essential substance of a conference paper presented at the AIEE summer technical meeting, St. Louis, Mo., June 29, 1944.

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The growing interest in electric house heating has stimulated much discussion concerning the amount of energy required and the cost of heating homes of different sizes and types of construction, the load demands of a heating installation, and the effect of this type of load on a distribution system. Data collected by the Tennessee Valley Authority and analyzed in this article help to answer some of these questions.

In order to help answer some of these questions, the Tennessee Valley Authority has collected considerable data on the operation of various heated homes located throughout the region and has made some analyses of these data in order to determine:

1. How this form of heat is accepted by the consumer from the standpoint of service and cost.
2. The amount of energy used for heating in well-insulated houses as compared with the amount required for heating uninsulated houses.
3. The load characteristics of electric house heating and its effect on the power demands of the distributor.
4. The value of this type load as a potential source of revenue.

It should be noted that this study is somewhat limited in scope. There are some factors which we have not been able to analyze up to the present time. Through extensive co-operation of various power distributors data on a number of typical electrically heated homes have been collected. These include information on the size of house, type of construction, family occupancy, heating installation, number of kilowatt-hours used, and load-demand records.

MANY FACTORS AFFECT HEAT REQUIREMENTS

Regardless of the method employed to provide heat in a house, certain basic principles must be observed. These have to do with the heat requirements and heat losses of the structure itself. Various materials and combinations of materials are employed in the construction of houses. The rate of heat transfer through walls, ceilings, and floors has been the subject of extensive laboratory tests; and data are available from a number of reliable sources by which the theoretical heat losses from any building can be computed. The actual amount of heat used in a given home, however, may vary quite widely from such theoretical quantities, because of different conditions of occupancy. The room temperatures maintained have been found to vary widely among different families. In some homes, occupied only by a husband and wife, with both working, little heat is used except in the evenings, while in other homes where there are small children or elderly persons, high temperatures are maintained throughout the day. Many families are careless about keeping outside doors closed, and often larger children pass into and out of the house frequently. These factors often affect quite substantially the amount of

Table I. Analysis of Electric-Heating Data Collected From 30 Residences of Various Sizes

Averages Per House in Size Groups

Number of Rooms	Volume (Cu Ft)	Connected Load (Kw)		Annual Kwhr		Annual Revenue	
		Total	Heating	Total	Heating	Total	Heating
4	6,240	28.79	13.98	14,848	9,314	$126.80	$ 48.50
5	7,500	31.00	16.50	16,910	10,186	134.84	52.14
6	11,780	37.60	23.00	22,399	15,551	174.78	89.58
9	11,922	50.50	32.90	23,751	17,763	184.39	105.07
Average per house	8,930	34.28	19.48	18,278	12,052	147.07	65.98
Average per room	1,685		3.67		2,274		12.40
Average per cubic foot			2.18 watts		1.35		0.0074

The group of 30 residences from which the above averages were derived consisted of ten each of the four- and five-room houses and five each of the six- and nine-room houses.

heat used. Irregular habits of living, absence from home, and differences in retiring time also have their effects on the heat requirements of a home. Often these have more influence on the amounts of energy used than the differences in degree-days.

The Tennessee Valley lies within a region which has moderate climate. The normal winters average about 3,200 degree-days. The degree-day is a unit based upon the temperature difference in Fahrenheit degrees between the mean temperature of the outside air over a 24-hour period and 65 degrees Fahrenheit. The mean temperature is the average of the maximum and the minimum temperatures of the period. In the southern portions of the area the normal heating season is 2,500 degree-days, and along the northern boundary a normal winter has around 4,000 degree-days.

THE MAJORITY OF HOUSES ARE SMALL

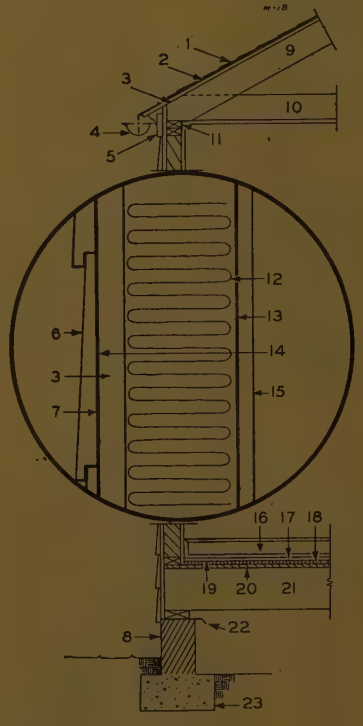
The application of electric heating has been confined almost wholly to what might be termed small homes. Such a home, typical of the majority, has a volume of heated space between 6,000 and 9,000 cubic feet. A few are as large as 12,000 cubic feet. A small four-room well-insulated house (Figure 1) will have a heat loss of about 35,000 Btu per hour based on a temperature difference of 70 degrees between the outside air and room temperature. This is 500 Btu per hour for each degree difference. The theoretical heating load for a 30-degree day, therefore, would be 15,000 Btu per hour, or 106 kilowatt-hours for a 24-hour day. With a heating season having 3,500 degree-days, such a house would require 12,600 kilowatt-hours. This is predicated on the assumption that all parts of the house would be heated to the same temperature all the time. In practice, however, this is never the case. Usually bedrooms are not heated to a temperature so high as that required for comfort in the other parts of the house. Much of the time no heat is used in the bedrooms. This practice results in less heat actually being used than the computed quantities.

THE AMOUNTS OF ENERGY USED

The average amount of energy estimated as being used for heating is shown in Figure 2. In ten four-room houses on which data were collected the average is 9,300 kilowatt-hours per heating season. This quantity was calculated from the records giving the amounts of energy actually used and taking the number of kilowatt-hours used during the summer months as a base from which the amount of energy used for nonheating purposes was calculated. The average amount of energy used for heating in a group of ten five-room houses is 10,200 kilowatt-hours. In five six-room houses the average was 15,500 kilowatt-hours per season and for the same number of nine-room houses



A. Typical small house designed for electric heating



B. Wall section of house

1—Roof	*13—Moisture barrier*
2—Felt	*14—Insulation, 0.06 coefficient*
3—Sheathing	*15—Plaster or plaster board*
4—Gutter	*16—Base*
5—Cornice	*17—Finished floor*
6—Siding or veneer	*18—Insulation board*
7—Resin paper	*19—Building paper*
8—Foundation	*20—Subfloor*
9—Rafter	*21—Joists*
10—Joist	*22—Termite strip*
11—Metal angle	*23—Footing*
12—Insulation	

Figure 1

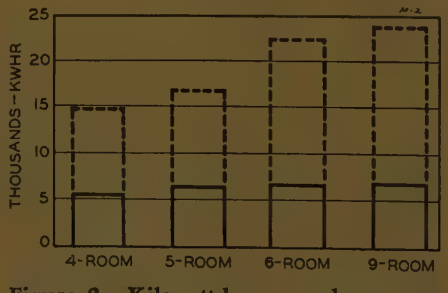


Figure 2. Kilowatt-hours used per year for heating and for other purposes in homes of various sizes

Dotted line—Heating
Solid line—Other uses

17,760 kilowatt-hours. The average for the 30 houses is 1.35 kilowatt-hours per cubic foot per season at a cost of 7.4 mills per cubic foot on the standard TVA residential rate. Additional data by house groups are shown in Table I. Further analysis of the data pertaining to the heat requirements of well-insulated houses as compared to those having poor or no insulation indicates that the energy required for heating a well-insulated house is from 20 to 35 per cent lower.

The cost of electric heat should not be compared to heating by other means on a "cost-of-fuel" basis alone. Users of electric heat are quick to point out that substantial savings in cleaning and redecorating result where this method of heating is used. The total construction cost of the house can be reduced through the elimination of space required for the heating plant, or else this space can be utilized for other purposes. Those who use electric heat praise it very highly.

INDIVIDUAL ROOM HEATERS OF THE RADIANT-CONVECTION TYPE USED

With only a few exceptions the electric-heating installations in the Tennessee Valley area employ individual room heaters of the radiant-convection type. Most of these are controlled automatically by thermostats built into each heater. This method of control provides for different temperature levels in various parts of the house. From the standpoint of the power distributor this has the advantage of improving the diversity of the load on the distribution system. The installation cost of these heating systems, including heaters, wiring, and prorated additions to the service and metering equipment averages about $25 per kilowatt.

In a six-room house, usually the total heating load will be divided among seven or eight units which are located in the various rooms. Except during a warming-up period, it is very seldom that all of these heaters will operate simultaneously. This results in improving the load diversity both in individual houses and in a group of houses.

TOTAL DEMAND IMPORTANT TO UTILITY

Inasmuch as it is the complete load and not the heating load alone which must be served by the power distributor, the total demand of any individual house or group of houses becomes of extreme importance. In order to arrive at some idea of the character of a daily load curve on "all-electric" homes having heating installations in addition to cooking, water heating, refrigeration, lighting, and miscellaneous appliances, recording instruments were installed in a group of six adjacent houses at Knoxville and in several individual houses in Chattanooga. All of these houses are of recent construction, and the electric-heating systems were installed when the houses were being built. The Knoxville group consisted of four four-

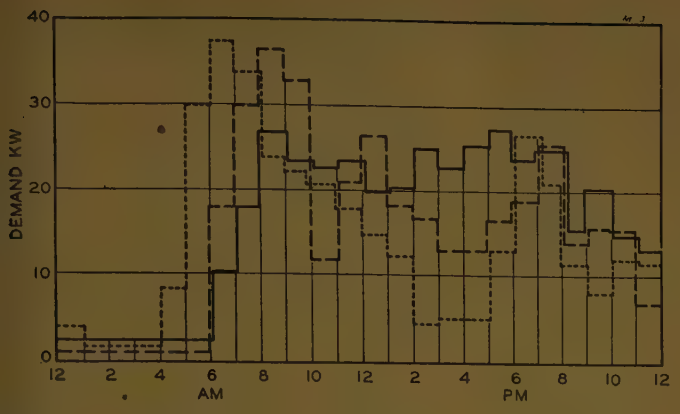


Figure 3. Demands on six electrically heated houses

February 6, 7, and 8, 1942
Solid line—Saturday; 28 degree-days
Dashed line—Sunday; 28 degree-days
Dotted line—Monday; 24 degree-days

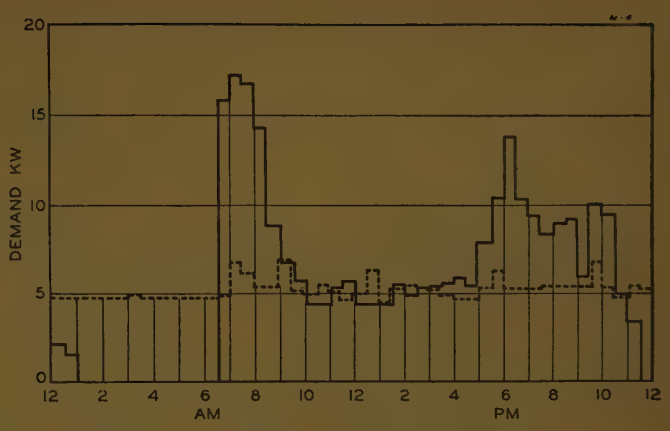


Figure 4. Typical daily load curves on two houses illustrating the effect of different schemes of operation

January 13, 1944; 30 degree-days; mean temperature 35 degrees Fahrenheit

Solid line—All heat off at night
138 kilowatt-hours—maximum demand 17.7 kw
29 per cent load factor 47 per cent demand factor
Dotted line—Part of heaters operated continuously on thermostats
126 kilowatt-hours—maximum demand 6.9 kw
76 per cent load factor 21 per cent demand factor

room and two five-room houses extremely well-insulated. The radiant-convection heaters are flush type installed in the outside walls, and the heating load totaled 70 kw for the six houses. In each house there were installed a range, water heater, and refrigerator in addition to lighting and miscellaneous appliances. The total connected load of the six houses was 162 kw, an average of 27 kw per house. The average total annual energy used for all purposes over a two-year period was 67,347 kilowatt-hours, or 11,224 kilowatt-hours per house. The portion of energy estimated as being used for heating was 42,279 kilowatt-hours, or 7,046 kilowatt-hours per house, which is 63 per cent of the total.

This group of six houses having a total connected load of 162 kw is being served satisfactorily by one 50-kva transformer. There are 52 houses in this one subdivision, all with electric heat. Each house is served through a 50-ampere 240-volt three-wire self-contained meter and a 100-ampere service switch. The circuits for the heaters are 230-volt and are run with number-12 conductor to the small units and number-10 conductor to the large units. The load is divided among eight transformers. These are arranged as indicated in Table II.

Demand readings were taken of group 2 for several days during the month of February 1942. These readings for three days are shown in Figure 4. The maximum 30-minute demand recorded during this test was 49.5 kw. This occurred between 8 and 8:30 a.m. The outside temperature at the time was 36 degrees Fahrenheit. The maximum hourly demand was 37.5 kw which occurred between 6 and 7 a.m., when the outside temperature was 28 degrees Fahrenheit. During a previous check made in January, a maximum 30-minute demand of 57.5 kw was recorded. At the time of this peak, the outdoor temperature was ten degrees Fahrenheit. The daily load curves shown are typical for this group. Similar data were obtained for several individual houses at Chattanooga. Typical load curves of two methods of operating the heating equipment are shown in Figure 5.

A six-room house having a total load of 37.5 kw with 23.0 in heating established a 30-minute demand of 17.7 kw January 13, 1944. The peak hourly demand was 17.5 kw. This peak occurred between 7 and 7:30 a.m. with an outdoor temperature of 31 degrees Fahrenheit. These demands were obtained by means of a graphic ammeter which indicated the instantaneous load. On the same day the maximum instantaneous demand reached 19.0 kw and lasted for about ten minutes. The load factor for this day was 29 per cent. An interesting contrast to this curve is one showing demands for the same day of another house located a short distance from the first. The total connected load in this house is 33.0 kw, and the heating load is 19.0 kw. The maximum 30-minute demand recorded for this house on that day was 6.9 kw, which is only 21 per cent of the connected load. The load factor for the 24-hour period was 76 per cent.

Further investigation developed the fact that in this house the family allowed the heaters in the living room and a hall to operate throughout the night, while in the other house all heaters were switched off when the family retired and turned on again next morning. The benefit to the power distributor of the all-night operation is obvious.

GOOD DIVERSITY OBTAINED

By selecting from a number of typical daily load curves showing hourly demands recorded when the degree-day requirements were approximately the same, a composite curve of ten houses was prepared. The maximum demand per house was found to be 6.6 kw or 21.7 per cent of the total average connected load. The period when the highest demands came falls between the hours 6 and 9 a.m. A slight increase in load at noon and part of the evening peak between 6 and 7 o'clock most likely are due to cooking operations. The daily load factor for this hypothetical group is fairly high, being 58 per cent.

Table II. Eight Transformers Serving 52 Electrically Heated Houses

Group	Transformer Rating (Kva)	Houses Served
1	50	8
2 (test group)	50	6
3	50	9
4	50	8
5	50	5
6	50	8
7	37	5
8	25	3
Total	362	52

Average per house—6.96 kva.

EFFECT OF HOUSE HEATING ON SYSTEM LOAD CURVES

In order to gain some idea of the effect which house heating may have upon the pattern of a system's load curve, a number of typical winter days was selected, and the hourly demands for distribution systems of various sizes were plotted. Upon these curves were superimposed hypothetical demands representing a house-heating saturation between three and five per cent. It is believed that these are an indication, at least, that while the effect would be somewhat different for each system, in general, there would be no great variations from the existing patterns.

There is, of course, the argument that house heating is a seasonable load and therefore an undesirable one. If one assumes the Knoxville group of houses to be typical, a curve has been prepared showing the number of kilowatt-hours used by months for a period of three years (Figure 6). It is obvious that the major portion of energy used for heating is confined to a period of three or four months.

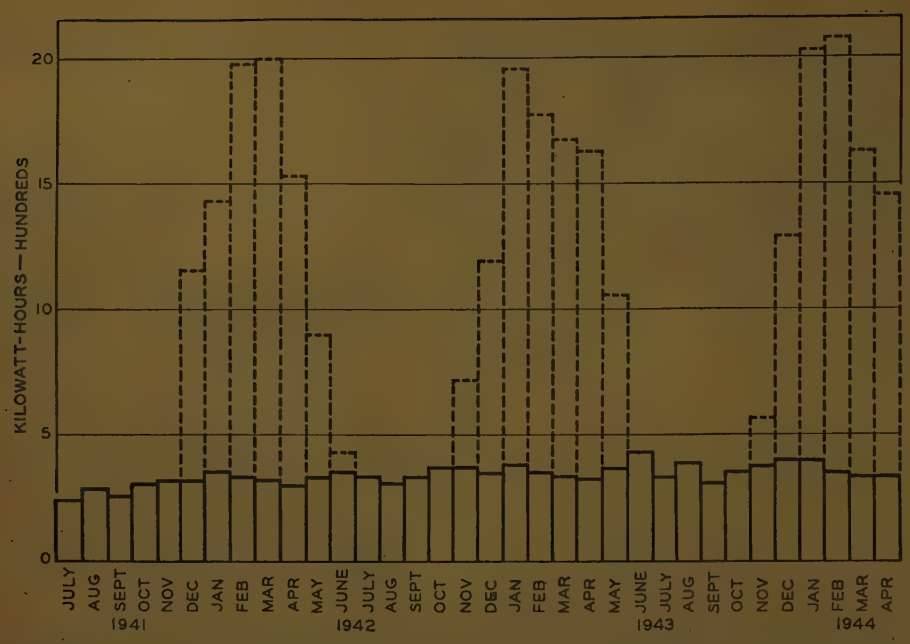


Figure 5. Graph showing the average number of kilowatt-hours used by months for all purposes in a group of six four-room and five-room well-insulated and electrically heated houses located on Bobwhite Lane, Knoxville, Tenn.

Dotted line—For heating
Solid line—For all other uses

This suggests a need for some type of load which would tend to build up the low use of the summer months. Air conditioning has been suggested as one probable means of doing this. While air-conditioning equipment would not use an amount of energy equal to that required for heating, its general use would help considerably in increasing the number of kilowatt-hours used in July, August, and early September, and in improving the annual load factor.

The use of reversed-cycle refrigeration to provide summer cooling and winter heating has been suggested as a probability. After the war we may see a limited application of the reversed-cycle principle in areas having moderate winters, but there are still some features about it which must be overcome before its use can be sufficiently widespread to make it important in the potential house-heating field. In the meantime a combination of space heaters and room-cooling units may become the first step to all-year air conditioning in the home. There are many indications of a growing interest on the part of architects, investment builders, and prospective home owners in electric heating.

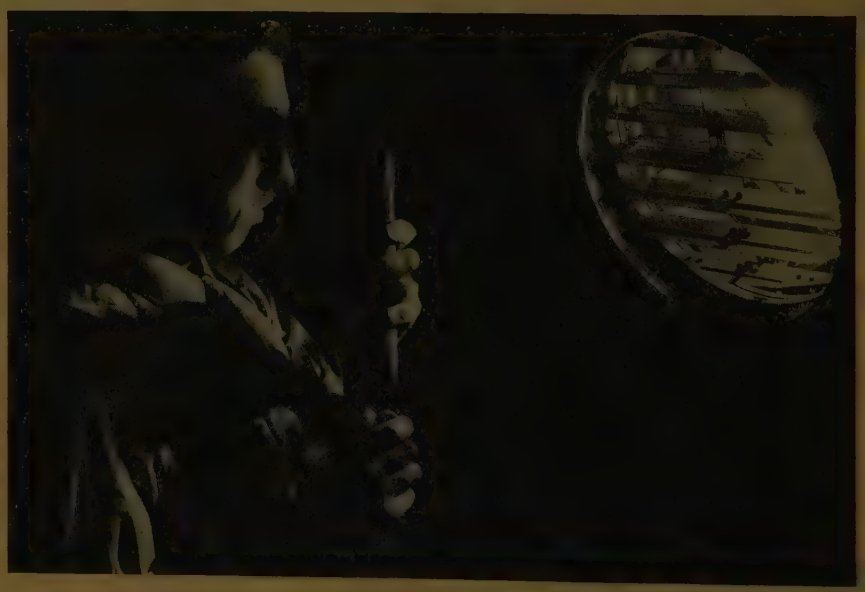
It would be unfortunate indeed if installations of electric heating were made in houses unsuited to their use. There is a danger that electric heating may be installed in old houses, or in houses that are not insulated, with results that would prove both expensive and disappointing. Most likely, if this were permitted, electric house heating would be seriously retarded, and it would reflect most unfavorably upon the industry as a whole.

CONCLUSION

The studies the results of which have been outlined in this article are not complete. It is hoped that much more comprehensive data can be developed when the war emergency is over. There should be more information, for example, on the effects of greater extremes of temperature variation than those encountered in the periods referred to herein. The effects of successive levels of saturation of space-heating installations on the costs of distribution will be a matter of direct interest to utilities and should be studied carefully. Nevertheless, the preliminary results which are already available are important in revealing that the costs to the consumer and the advantages to him of electric heat are such that in some areas it will be a real factor in the load-development picture for the postwar period. While there is a good deal of work still to be done in order to determine the total effect of this load on costs of service, it has been learned that under many conditions there is a high degree of diversity with consequent lowering of the distribution costs. Some distributors of TVA power already have found reason to believe that the space-heating load is a profitable one.

Searchlight Supplements Radio Communication

Specially designed by the Westinghouse Electric and Manufacturing Company to withstand a degree of rough service required of few shipboard lights, this searchlight is here being tested for the intensity of its beam, which should be visible for 17 miles. Mounted on the open decks of United States battleships, cruisers, and destroyers, the signal light substitutes for radio communication to airplanes, shore patrols, and other ships, when radio signals might be intercepted by the enemy or are ineffective because of weather conditions. The glass of the lens, ten times stronger than plate glass, will not shatter under the combined and opposite forces of the heat of the lamp and the cold of icy sea water, nor from the blast of giant guns. The shutters which blink out the messages have been opened and closed a total of 2,000,000 times during testing.



The ECPD Proposed Canons of Ethics

DUGALD C. JACKSON
FELLOW AIEE

For some years efforts have been made to establish a uniform code of ethics that would be acceptable to all branches of the engineering profession. The project is now under sponsorship of Engineers Council for Professional Development; its history and present status are outlined here.

THE TERM "professional ethics" is commonly used quite loosely but, in fact, it means "rules of right conduct in which each item is fundamental to social relations" within the scope of professional activities. It is often said that engineering is not a profession because it has been unable to agree upon a suitable code of ethics which applies through the field of all of our great engineering societies. The lawyers, through the American Bar Association many years ago, adopted a code of ethics which they have modified and added to in the interval and which has 47 items. This code is published by the Association under the title "Canons of Professional Ethics" in a small pamphlet, along with some additional matters relating to judicial ethics. The doctors also, through the American Medical Association, have their code published by the Association in a pamphlet called "Principles of Medical Ethics," containing 42 items. Each lawyer and physician usually possesses a copy of the pamphlet relating to his profession and cherishes it as of importance in his active life.

The engineering societies have never been able to come to such solidarity of views. Most of the important engineering societies possess codes of ethics, but the different codes cover different ground and even where items are similar in content, the phraseology may be quite different, which makes the whole situation of ethics in the engineering field quite confusing or even chaotic.

Several years ago officers of two of the Founder Societies urged the formation of a committee to enter upon the formulation of those ethical principles which would be appropriate to the entire engineering field. The organization of this committee was undertaken by the President of the American Engineering Council (AEC), which then existed and which had at its head Alonzo J. Hammond, a Past-President of American Society of Civil Engineers. Each of the national societies constituent to AEC was asked to appoint through its board of directors a member of an engineers' joint committee on ethics. This was done, and the committee has steadily gone forward in the effort to formulate a code of ethics which is adequate in the field of engineering and yet is sufficiently specific to be a sound directive to the young engineers as well as an inspiration for the young and the old.

In this formulation, it has been the effort to include only items which are definitely rules of right conduct that are fundamental in social relations and that are frequently enough violated in engineering relations to make it desirable to have the rules in formal adoption, at the same time avoiding features of practice which may be changed as a consequence of current modification of customs and also avoiding features that are dealt with as matters of law through statutory processes or are dealt with as tenets of religion.

The AEC, which had been supported by the Founder Societies, was dissolved and the Engineers' Council for Professional Development (ECPD) undertook the sponsorship for this committee. Therefore three additional societies, which were constituents of ECPD but had not been constituents of AEC, added their representatives to the committee. The group of societies now comprises the following: American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, The American Society of Mechanical Engineers, American Institute of Electrical Engineers, The Engineering Institute of Canada, The Society for the Promotion of Engineering Education, American Institute of Chemical Engineers, National Council of State Boards of Engineering Examiners, and American Institute of Consulting Engineers.

The representative of each society is a man well-known in engineering life, and most of the individuals are past presidents of the society represented or former chairmen of its committee on ethics or committee on professional conduct. In certain instances the representative is both a past president and a former or present chairman of his society's committee on ethics.

The committee finally approved a formulation containing 31 items. This formulation is denominated "Canons of Ethics for Engineers." Here the word "canons" is used instead of the word "code" because many of the engineering societies publish codes of practice that are purely questions of science or of law or of convenience, and it seems desirable to distinguish clearly between the ethical principles and the codes of practice. The precedent of the American Bar Association is a perfectly good one for the use of the word "canons" in this situation.

The 31 items were reported to the ECPD for its consideration, and after discussion Council referred the proposed canons, with the proposal for adoption, to its constituent societies for consideration by their governing boards.

It should be remembered that the ECPD has no authority of itself to impose practices on the engineering field, but it is a representative organization established for the purpose of proposing measures for improving engineering relations and recommending the same to its constituent societies. Then, if the constituent societies approve, the ECPD can proceed with the introduction of the idea as a fixed process in the engineering field.

The action of ECPD was to lay the proposed canons of ethics before its constituent societies with the request for comments or instructions relating to the formulation, and particularly for recommendations of elimination of any items which in the opinion of any board might appropriately be omitted or recommendations of any additional items which in the opinion of any board ought to be included but are omitted in the submitted draft.

Much urgency has been expressed for a very brief code of ethics, partially on the ground that it would be more convenient for printing with various papers, or the ground that it would be easier for young engineers to familiarize themselves with the items; but it may be justly stated that matters of convenience are not fair grounds for judgment of ethical principles. All wish for brevity in such a formulation, but we should not hesitate to have as many items as are appropriately needed for our situation.

If a formulation is finally and formally adopted, it may be assumed that the ECPD will have a pamphlet printed which may be provided to their members by the engineering societies in a manner analogous to the manner of circulation of the pamphlets previously referred to which are issued by the American Bar Association and the American Medical Association. Then each engineer would have in his possession a copy of the pamphlet which he could cherish and utilize in a manner which he felt most desirable.

At the present time the board of directors of the AIEE has approved the ECPD proposed canons of ethics, with suggestions of

Dugald C. Jackson, professor emeritus of electrical engineering, Massachusetts Institute of Technology, Cambridge, Mass., is chairman of the committee on ethics of Engineers Council for Professional Development; he was president of AIEE 1910–11.

moderate changes of phraseology. The same is true of the council of the Society for the Promotion of Engineering Education (SPEE). The American Institute of Consulting Engineers (AICE), after very careful study by its council, has approved the formulation with the suggestion of very considerable changes in the order of the items and modifications of phraseology. The ASME council has indicated that it desires to co-operate with other societies in securing a uniform code of ethics for the engineering field, and it approves in principle the ECPD proposed canons, leaving it now for its committee on professional conduct to advise with it as to any recommendation which the council should make to ECPD. The board of direction of ASCE has the matter still under consideration as also has the council of the Engineering Institute of Canada.

The Detroit Engineering Society, which is recognized as a well-organized and well-managed local society, has adopted the ECPD canons, and these canons are under consideration by other local societies.

The AIEE has taken an important place in leadership, along with SPEE and AICE, in the approval of these proposed uniform Canons of Ethics for Engineers.

Proposed Canons of Ethics for Engineers

FOREWORD

Justice, courtesy, and sincerity, exercised with honesty, wisdom, and mutual interest between men, make the foundation of ethics.

This ethical code is prepared for the guidance of engineers and engineering students and is aimed to express a consensus of considered judgment on certain matters of common interest to engineers, especially such matters as may not have obvious ethical solutions. Ethics should be more than passive observance of a code of "don'ts" in the life of an engineer. They should be recognized as dynamic principles guiding his conduct and his way of life.

The principles are briefly set forth in what follows.

PROFESSIONAL LIFE AND EMPLOYMENT

1. The engineer will avoid conduct and practices likely to discredit the honor and dignity of the engineering profession.

2. He will attempt to co-operate in building up the engineering profession by interchanging information and experience with other engineers and students and by contributing to the work of engineering societies, schools, and the scientific and engineering press, without disclosing confidential matter.

3. He will present clearly the consequences to be expected from the deviations proposed if his engineering judgment is overruled by nontechnical authority in cases where he is responsible for the technical adequacy of engineering work.

4. He will endeavor to protect the engineering profession and all reputable engineers from misrepresentation and misunderstanding.

5. He will take care that credit for engineering is attributed to those who, insofar as his knowledge goes, are the real authors of the work.

6. He will maintain the principle that unduly low compensation for engineering employment tends toward inferior and unreliable results and is to the disadvantage of his profession.

7. He will not advertise his work or merits in a self-laudatory manner or in a way injurious to the dignity of his profession.

RELATIONS WITH OTHER ENGINEERS

8. The engineer will not, directly or indirectly, injure the reputation or business of another engineer.

9. He will not try to supplant another engineer in a particular employment after becoming aware that decision to employ the other has been reached.

10. He will not compete with another engineer on the basis of charges for work by underbidding after he has been informed of the charges named by the other.

11. He will not use personally the advantages of a salaried position to compete unfairly with another engineer.

12. He will not knowingly review the work of another engineer, for the latter's client or employer, without the other engineer's knowledge unless the latter's connection with the work has terminated.

13. He will not knowingly become associated in responsibility for engineering work with engineers who do not conform to ethical practices.

RELATIONS WITH CLIENTS AND EMPLOYERS

14. The engineer will endeavor, insofar as it is possible, to secure justice between his client or employer and the contractor when dealing with contracts.

15. He will act in professional matters for each client or employer as a faithful agent or trustee.

16. He will not accept compensation, financial or otherwise, from more than one interested party for the same service, or for services pertaining to the same work, without the consent of all interested parties.

17. He will not, without the full knowledge and consent of his client or employer, have an interest in any business which may bias his judgment regarding engineering work for which he is employed or which he may be called upon to perform, or have an interest in a business which may compete with the business of his client or employer.

18. He will not be financially interested in the bids as a contractor on competitive work for which he is employed as an engineer unless he has the consent of his client or employer.

19. He will not accept commissions or allowances, directly or indirectly, from contractors or other parties dealing with his client or employer.

20. He will make his status clearly understood to his client or employer before undertaking an engagement if he may be called upon to decide on the use of inventions, apparatus, or any other thing in which he may have a financial interest.

21. He will regard it his duty to guard against dangerous elements in apparatus, structures, or plant, or dangerous conditions of operation therein, and upon observing such conditions in work with which he is associated, he will call them to the attention of his client or employer. If dangerous conditions persist with his knowledge, he is not fully relieved of his responsibilities.

22. He will, when he is a public officer, recognize his limitations and, under such conditions, retain and co-operate with other engineering experts and specialists whenever such co-operation may be serviceable.

RELATIONS WITH THE PUBLIC

23. The engineer will interest himself in the public welfare and be ready to apply his special knowledge, skill, and training for the benefit of mankind.

24. He will assist public officials and others in attaining a fair and correct general understanding of engineering matters, extend the public knowledge of engineering, and discourage untrue, unfair, and exaggerated statements regarding engineering.

25. He will recognize the fact that meetings of engineering societies and the engineering press provide the proper forum for technical discussions and criticisms, and also that clear statements of facts relating to engineering enterprises are often of value to the public when they are prepared for laymen's understanding and released by competent authority through the public press.

26. He will not issue *ex parte* statements, criticisms, or arguments on matters connected with public policy which are inspired or paid for by private interests unless he indicates on whose behalf he is making the statements.

27. He will not express publicly an opinion on an engineering subject without being informed as to the facts relating thereto.

28. He will express no opinion which is not founded on adequate knowledge and honest conviction while he is serving as a witness before a court, commission, or other tribunal.

29. He will not lend his name to any questionable enterprise or engage in any occupation contrary to law.

30. He will make provisions for safety of life and health of employees and of the public who may be affected by the work for which he is responsible.

31. He will carry on his work in a spirit of fairness and loyalty to associates, subordinates, and employees, fidelity to the public needs, and devotion to high ideals of courtesy and personal honor.

Prepared by a joint committee from national engineering societies (American Institute of Chemical Engineers, American Institute of Consulting Engineers, American Institute of Electrical Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Civil Engineers, American Society of Mechanical Engineers, Engineering Institute of Canada, National Council of State Boards of Engineering Examiners, Society for the Promotion of Engineering Education) with sponsorship of Engineers' Council for Professional Development.



VIII—Influence of Research on Engineering Developments

L. W. CHUBB
FELLOW AIEE

Between World Wars I and II the value of scientific research was recognized in the United States. The years of normal research which have been compressed into a period of months in World War II are a tremendous factor in the winning of the war and will have a profound effect on the postwar world. The many war developments which are the result of research in the electrical and other fields may be expected to revolutionize the electrical industry.

IT IS HAZARDOUS to make predictions; yet it is proper at this time to consider the effect of current and postwar research on engineering developments. In spite of the glaring contrast between the role of scientific research in the period of the last war and its part in the present conflict, the experiences following World War I offer the most helpful standard in present evaluations of postwar activity.

Before the last war American scientific research lagged far behind that of our chief enemy. We were industrially vigorous; we had outstanding inventers and engineers; but our research was relatively weak in every field except agriculture. There were a few good research laboratories in industry and creditable activities in some of the leading universities. On the whole, however, science was not appreciated as the foundation of progress in engineering, and the ease with which the material needs of a nation opulent in natural resources could be satisfied at home or abroad fostered this attitude. A research career was not particularly attractive to the university student, and, consequently, trained scientific workers with advanced degrees were lacking. Since the universities alone train such men, there was no hope of keeping up with foreign research.

The first war has been called an engineer's war. It was a period of great engineering expansion, but the expansion was limited mostly to techniques already established. Thus, we still depended on imports from Chile for practically all of our nitrates. The airplane was still in its infancy and could not be relied upon even for observation, since a sortie over enemy lines was but an invitation for a dog fight. The radio, although useful, was a crude instrument. Existing knowledge was exploited in its design and application, but little advanced work was undertaken. As a matter of fact, practically no American radio supplies gave service at the front before the Armistice was signed. Automotive transport may be taken as a final indication of technological primitiveness: the art was so immature that our armies had to depend upon rail installations for subsistence and supplies.

Recognition of the necessity of scientific research did not come until we had entered the conflict. Soon after 1917 groups such as the Naval Consulting Board, the National Research Council, and the Chemical Warfare Service were formed to consider and develop new instruments for offense and defense. Some of the most urgent problems which received considerable attention, typical of the research activity at that time, were gas warfare, sound ranging, and submarine detection. Work on new and more effective poisonous gases and on defensive measures against such gases was pursued with considerable success. The location of enemy gun batteries by the sound of their blasts was another problem solved at this time. Then, as in this war, the submarine became a great menace to shipping. Methods of detecting these undersea prowlers were investigated by scientists throughout the country, but the work was not co-ordinated properly.

Much good work was done by the groups of scientists and engineers before the end of the last war. But, in general, improper co-ordination, too little, and too late, characterized the fruit of their work. Nevertheless, the seeds of postwar research had been sown, and the stimulation rather than the immediate results of this war research was responsible for the great scientific and technical advances from which new materials, new processes, new products, and major industries emerged.

In the peace between 1918 and Pearl Harbor, an enormous change in attitude toward research took place. There was a more abundant supply of men with scientific training; there was more research in the universities; industrial-research laboratories increased threefold; and many research foundations were founded to provide new knowledge for engineering applications. Today, the public expects and looks for change and progress; advances in any field are items of news; and any industry or company that fails to utilize new discoveries and new techniques is soon hopelessly out of date.

In contrast with the last war, steps were taken even before Pearl Harbor to mobilize the scientific workers of the nation. The blitz tactics, said to be an American idea adapted by Germany, called for radically different tools of combat, defense, communication, and transport. Fortunately, we had two years for observation and preparation before formally declaring war. The need of further developments in science was evident, particularly in engineering, physics, metallurgy, and chemistry. Practically all branches of scientific endeavor have been called upon to furnish the allies with the devices and supplies needed to meet the

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present situation. Existing statutes, creating the American Academy of Sciences and the National Research Council and allowing appropriations for scientific research and development, were fortunately available. Vast sums were granted through the newly organized Office of Scientific Research and Development to carry on scientific development of instruments of war. These organizations brought together large groups of research men. The leading scientists in the country were provided with the necessary laboratory facilities to plan and execute an enormous amount of research work. In addition to the work in the special laboratories, OSRD has contracted for research and development, on a cost basis, in many industrial and college laboratories. Similarly, groups including the Army and Navy have contracted for development work in the laboratories of industry where production could immediately and efficiently follow. Never before in the United States nor, indeed, in any other country, has such intensive scientific activity been so greatly and successfully instigated.

It has often been said that at least ten normal years of research have been accomplished in the last two years. This is probably a conservative estimate, particularly when we consider the prompt application of the ideas and discoveries. Conception, research, development, engineering, production, and utilization often have been compressed into a period of months rather than the traditional seven to ten years from test tube to market.

There can be no doubt that this vast undertaking has been a vital factor toward the winning of the war and that the new knowledge gained will have a profound influence upon postwar developments. Besides the weapons, the defense methods, and the physical equipment emerging from war research, many new and valuable materials, processes, and scientific principles will be available for engineering application.

If our peacetime developments are to be measured by the wartime accomplishments, the future will be a bright one. By comparison the results of 1917–18 are dwarfed; yet the technological advances following that conflict were climaxes in our industrial and social history. The present postwar engineering outlook promises to be more epochal, but there probably will be some delay this time in the application of new research to new products and services. The last war produced no tremendous, immediate market for consumer's goods, because no serious curtailment of such goods was necessary in our relatively short engagement in the war. The present necessary curtailment has created a vast and urgent demand. This demand for goods and services, coupled with the task of maintaining employment at a maximum, requires a prompt reconversion of industry. Old models in production or those which were just coming into production and for which tools are available, certainly should fill these needs and facilitate the transition from war to peace.

The future of the electrical industry, a typical one in many ways, suggests a few of the technological advances which present and subsequent research affords. During the war, the electrical industry has been contributing on two principal fronts. Power equipment and heavy machinery have been limited to the production front and to Naval installations. Electric equipment for communication, auxiliary devices, control, regulation, instrumentation, and low-power machinery have been serving on the firing line, in the air, on the ships—in any installation of consequence throughout the world. The research and development necessary for obtaining the most effective weapons has been stupendous and has disclosed new scientific information, new circuits, new devices, and new principles of operation which will serve as the basis of engineering applications for years to come.

Restrictions limit the information which can be given concerning many of the products resulting from research work. However, a mere mention of some of the fields of activity suggests some of the things useful in war and applicable in peace—electronics, radio, radar, gunfire control, stabilization of guns and moving equipment, servomechanisms, navigation aids, and control devices. A great many of the war developments will find almost instant application with little or no further modifications. Radar is a good example. With it our airplanes and ships may travel at scheduled speeds, assured of safety in fog or darkness. Other techniques utilizing ultrashort radio waves can be adapted to communications, and the increased number of frequency bands, beam transmission, and limitations of range will help eliminate interference. The general adoption of short-wave communication for load dispatching, supervisory control, and remote metering in our great utilities is reasonably certain. The very successful servomechanisms, control equipment, and electronic devices can be applied in peacetime with little additional development or engineering attention.

In the electrical industry, there will be no major problem of conversion as far as capital goods are concerned. Installed capacity has been greatly increased for government and war purposes. In spite of this, there will be new requirements in the power field to meet the demand of industries released from governmental restrictions and engaged in their peacetime production—production which will be stimulated by the war research and development in all fields.

Research in other than electrical fields has produced information and materials which also will be valuable in the electrical industry. New and more stable lubricants will find general application. New high-strength high-temperature alloys developed for superchargers and gas turbines will permit the use of higher temperatures in steam turbines. New compositions of matter having unusual properties will be useful as high-temperature insulation and cooling media; and many metallurgical products and methods of fabrication, in the long run, will revolutionize our industry with entirely new appliances and better more efficient machinery.

Although the predictions of the new life in the world to come immediately after the war have been exaggerated greatly, the outlook for the future is exceedingly bright; and it seems fortunate that the empty shelves and the accumulated buying power will permit great industrial activity immediately after the surrender and allow time for the engineers to remold the world with the new scientific knowledge of which there is an abundance.

Apparatus Imitates Sea Motion



Bolted to the steel table at the left, sea-borne equipment is rocked back and forth by this Western Electric Company testing apparatus as it would be during an ocean voyage

INSTITUTE ACTIVITIES

Winter Technical Meeting to Feature Wartime Developments

Arrangements are being made for the winter technical meeting, which will be held in New York, N. Y., January 22–26, 1945, with headquarters in the Engineering Societies' Building. Many wartime developments with interesting postwar applications will be presented in some 32 technical sessions and conferences. Also during this meeting the Edison Medal will be presented. In addition the Institute of Radio Engineers will hold their winter technical meeting concurrently, January 24–27, to permit engineers interested in radio and communication work to attend both meetings in one trip. The programs of both meetings will be co-ordinated, and as in the past a joint AIEE–IRE evening session is planned.

TECHNICAL SESSIONS AND CONFERENCES

Among the sessions and conferences which present wartime developments with postwar applications are two air-transportation sessions; sessions on industrial control, industrial power applications, and arc welding; a conference on wartime practices on distribution systems and their effect on operation and system design; and a conference on statistical methods applied to quality control. The development of the propeller milling machine, which has helped improve and accelerate the ship program, will be presented in two papers. Formerly the cutting of the spiral helix of a propeller blade was an arduous hand operation. The development of this machine tool has been made possible only by the close co-operation of electrical engineers and mechanical engineers. In another paper basic Selsyn systems and the characteristics of their components with servo mechanisms will be described. This type of control has had many military and industrial applications with new developments in the older principles and new applications of modified designs. Power-station auxiliaries, carrier-current applications, relays, circuit breakers, electrolysis, lightning, and insulation will be the subjects of other sessions of particular interest in the central-station field. Details will be given in the January issue as the program develops.

SMOKER

To afford recreation and an opportunity to get together with friends in pleasant surroundings a smoker will be held Tuesday evening, January 23, at the Hotel Commodore. Insofar as possible all of the features of this affair will be provided as in previous years. Tickets, including the dinner and show, will be six dollars per person, including tax. Tables for ten can be reserved by advance payment. Checks should be made out to "Special Account, National Secretary, AIEE."

THEATER TICKETS

Every effort is being made to have choice tickets available for the best shows. These are being reserved for the evenings of Wednesday, January 24, and Thursday, January 25. Preference will be given to requests from out-of-town members. Members desiring tickets for specific performances on these or other dates may write to Institute headquarters enclosing checks made out to "Special Account, National Secretary, AIEE." The theater-ticket committee will do its utmost to take care of such requests.

A limited number of seats are now available for "Oklahoma," "Song of Norway," and "Voice of the Turtle." Ticket prices follow:

Song of Norway	$6.00
Voice of the Turtle	4.20
Oklahoma!	4.80

COMMITTEES

The personnel of the 1945 winter technical meeting committee, which is making the arrangements, is as follows: J. F. Fairman, chairman; F. A. Cowan, W. S. Hill, M. D. Hooven, A. E. Knowlton, C. S. Purnell, C. C. Whipple, R. J. Wiseman.

AIEE Industrial Distribution Report Available Soon

The Institute's committee on industrial power applications, working under the chairmanship of J. Grotzinger, of the Goodyear Tire and Rubber Company, has been at work for some time preparing a report on "Electric-Power Distribution for Industrial Plants." This new publication will be available shortly.

The report co-ordinates the contributions of some 40 specialists from the following industries: Aircraft, automobile, cable, chemical, copper, electric equipment, factory insurance, mining, oil, photographic equipment, rubber, steel, and the utilities. It is intended to promote the use of sound engineering principles in the design of electric-power-distribution systems for industrial plants and in the selection of equipment for those systems. For the purposes of the report the distribution system is considered as extending from the point of entrance of the power-company service or the plant generating bus to the terminals of the utilization devices. This includes high-voltage substations, distribution substations, feeders, circuits, switch centers, and protective equipment. The committee wishes it fully understood that the report is not an AIEE Standard, and the recommendations are not intended in any way to be mandatory or restrictive.

The function of an industrial-plant distribution system is to receive power at one or more bulk power sources and deliver it to the individual utilization devices or loads, such as motors, lights, welders, and furnaces. The effectiveness with which a distribution system fulfills this function is measured in terms of voltage regulation, service continuity, flexibility, efficiency, simplicity, maintenance, and cost. The proper distribution system to use in an industrial plant is the one which will provide adequate, economical, and safe electric service to the plant loads now, and also with an eye to the future. The selection of the system to be used involves consideration of not only adequate service to existing loads and first cost, but also the cost of operation, maintenance, and modification of the system to meet future changing loads. At no time should a saving in cost be made by a sacrifice in reasonable safety to life or property. The report within its scope analyzes the service requirements, voltage-regulation problems, load characteristics, and distribution-system characteristics, and it also deals with the selection of equipment for distribution systems. Operating and maintenance problems are dealt with only insofar as they affect system and equipment selection.

To give a comprehensive picture of the vast amount of valuable data made available to industrial engineers through the publication of this report would require more space than is here available. A listing of the six chapter headings, however, will give a brief view of the scope of the work. They are as follows: System planning; primary substations and feeders; transformers, primary switchgear, and low-voltage feeder protection; low-voltage feeders, panel boards, bus distribution systems, and load circuits; fault-current calculations; wires and cables.

The Institute's committee on industrial power applications believes that this publication fills a long-felt need in the industrial-engineering field and may be the forerunner of similar reports covering other phases of industrial engineering. The new report, now on the press, is a pamphlet of 112 pages with very heavy paper cover. It is profusely illustrated. Copies will be available from AIEE headquarters at one dollar each with discounts permitted on quantity orders.

1944 December Supplement Contents Announced

Technical papers presented before AIEE technical meetings during 1944 and not published in the monthly *Transactions* section of *Electrical Engineering* or in the June supplement will appear in the December 1944 "Supplement to Electrical Engineering—Transactions Section." The December supplement will contain 45 technical papers, discussions of those papers, and discussions of the papers already published in the July–December monthly sections. Contrary to the announcement made in the September issue of *Electrical Engineering* there will be only one year-end supplement because, although there is a large amount of material to be published, it is not enough to necessitate two supplements as had been anticipated. Issuance of the December supplement will complete publication of papers and discussions presented before the 1944 North Eastern District meeting in Boston, Mass., April 19–20; the 1944 summer technical meeting in St. Louis, Mo., June 26–30; and the Los Angeles technical meeting in Los Angeles, Calif., August 29–September 1.

Copies of the supplement will not be available until some time after the first of the

year, at which time published copies will be mailed to those who entered advance orders. Others then may obtain copies at 50 cents each from the AIEE order department, 33 West 39th Street, New York 18, N. Y.

Papers appearing in the December 1944 supplement, abstracts of which have been published in *Electrical Engineering* in advance of the meetings are:

Air Transportation

44-81—A-C and D-C Short-Circuit Tests on Aircraft Cable; *J. C. Cunningham (A'42), W. M. Davidson (A'42).* Abstracted in April 1944 issue, page 145.

44-82—Damping Cylinders for Aircraft Gyroscopes; *F. R. Sias, J. H. Wright (M'43).* Abstracted in April 1944 issue, page 146.

44-150—Design Considerations in Aircraft Instruments to Meet War Service; *C. V. Savage (M'43), J. M. Whittenton (A'37).* Abstracted in the June 1944 issue, page 226.

44-180—Alternating Versus Direct Current for Aircraft-Radio Power Supply; *D. E. Fritz (A'43), C. K. Hooper.* Abstracted in the August 1944 issue, page 309.

44-183—Electrically Heated Clothing; *G. H. Wotring.* Abstracted in the August 1944 issue, page 309.

44-189—Design of an Ignition System for an 18-Cylinder Aircraft Engine; *J. R. Harkness.* Abstracted in the August 1944 issue, page 310.

44-194—Aircraft-Electric-Accessory-Vibration Investigation; *D. R. Miller (A'42).* Abstracted in the August 1944 issue, page 310.

44-203—A 120-Volt D-C Aircraft Electric System; *L. M. Cobb (A'34).* Abstracted in the August 1944 issue, page 311.

44-205—Problems in Applying Protectors to Electric Aircraft Motors; *L. W. Buell (M'44).* Abstracted in the August 1944 issue, page 311.

44-223—Cable Used for Transmitting Electric Energy in Airplanes; *M. F. Peters, J. J. Phillips, Max Kronstein, H. B. Jealous.* Abstracted in the August 1944 issue, page 313.

44-225—A-C Supplies for Services in Large Aircraft—a British View; *E. J. Earnshaw, J. B. Shearer.*

44-227—Aircraft-Engine-Accessory Vibration; *John Tyler.*

44-229—Plastics in Aircraft Electricity; *E. B. Cooper.* Abstracted in the August 1944 issue, page 313.

44-231—New Test Chambers for Aircraft Electric Apparatus; *E. R. Summers (A'38), J. F. Settle (A'42).* Abstracted in the August 1944 issue, page 313.

44-233—Airplane Engine and Propeller Test-Cell Lighting; *D. H. Tuck.* Abstracted in the August 1944 issue, page 313.

44-237—Impedance of 400-Cycle Three-Phase Power Circuits on Large Aircraft and Its Application to Fault-Current Calculations; *C. K. Chappuis (A'41), L. M. Olmsted (M'39).* Abstracted in the August 1944 issue, page 313.

44-241—Design Considerations for D-C Aircraft Generators; *J. D. Miner, Jr. (M'42).* Abstracted in the August 1944 issue, page 314.

Communication

44-126—Developments in the Field of Cable and Radiotelegraph Communications; *Haraden Pratt (F'37), J. K. Roosevelt (M'30).* Abstracted in the June 1944 issue, page 227.

44-127—American Telegraphy After 100 Years; *F. E. d'Humy (F'30), P. J. Howe (A'09).* Abstracted in the June 1944 issue, page 227.

44-128—Telegraphy in the Bell System; *J. A. Duncan, R. D. Parker (M'14), R. E. Pierce (M'30).* Abstracted in the June 1944 issue, page 227.

Domestic and Commercial Applications

44-154—Load-Calculation Procedure for Electric-Panel Space Heating; *B. F. Raber, F. W. Hutchinson.* Abstracted in the June 1944 issue, page 228.

Electric Machinery

44-169—Progress in Impulse Testing of Transformers; *J. H. Hagenguth (M'44).* Abstracted in the June issue, page 229.

44-176—Differential Leakage With Respect to the Fundamental Wave and to the Harmonics—Integral-Slot and Squirrel-Cage Windings; *M. M. Liwschitz (M'39).* Abstracted in the June 1944 issue, page 229.

Electric Welding

44-105—The Design of Low-Voltage-Welding Power Distribution; *C. A. Adams (F'13), J. R. Fletcher (A'36), A. C. Johnson (A'42).* Abstracted in the June 1944 issue, page 229.

44-125—An Improved Electronic Control for Capacitor-Discharge Resistance Welding; *H. J. Bichsel (A'43), E. T. Hughes.* Abstracted in the June 1944 issue, page 230.

Electrochemistry and Electrometallurgy

44-100—Treatment and Casting of Metals by Electromagnetic Forces; *W. W. Hoke.* Abstracted in the June 1944 issue, page 230.

44-165—Characteristics of Chlorinated Impregnants in D-C Paper Capacitors; *L. J. Berberich (M'36), C. V. Fields (A'41), R. E. Marbury (M'36).* Abstracted in the June 1944 issue, page 230.

Electronics

44-11—Analysis of Rectifier Circuits; *E. F. Christensen (A'39), C. H. Willis (F'42), C. C. Herskind (M'40).* Abstracted in the June 1944 issue, page 230.

44-78—Development of Excitron-Type Rectifier; *H. Winograd (M'39).* Abstracted in April 1944 issue, page 146.

44-145—Design of an Electronic Frequency Changer; *C. H. Willis (F'42), R. W. Kuenning (A'42), E. F. Christensen (A'39), B. D. Bedford (M'43).* Abstracted in the June 1944 issue, page 231.

Industrial Power Applications

44-144—The Electronic Converter for Exchange of Power; *F. W. Cramer (M'40), L. W. Morton (A'38), A. G. Darling (M'44).* Abstracted in the June 1944 issue, page 231.

44-236—Vacuum-Tube Radio-Frequency Generator—Characteristics and Applications to Induction Heating; *T. P. Kinn (M'44).* Abstracted in the August 1944 issue, page 314.

Instruments and Measurements

44-161—A Resonant-Cavity Method for Measuring Dielectric Properties at Ultrahigh Frequencies; *C. N. Works (A'40), T. W. Dakin, F. W. Boggs.* Abstracted in the June 1944 issue, page 232.

Marine Transportation

44-159—Fault Protection on Shipboard A-C Power-Distribution Systems; *H. G. Rickover (M'44), P. N. Ross.* Abstracted in the June 1944 issue, page 232.

Power Transportation

44-106—The Effect of Weather on the System Load; *H. A. Dryar (M'43).* Abstracted in the June 1944 issue, page 232.

44-107—Conditions Controlling the Economic Selection of Prime Movers; *B. G. A. Skrotzki.* Abstracted in the June 1944 issue, page 232.

Power Transmission and Distribution

44-114—The Measurement of Lightning Currents in Direct Strokes; *G. D. McCann (M'44).* Abstracted in the June 1944 issue, page 233.

44-115—Transmission-Line Electric Loadings; *S. B. Crary (M'37).* Abstracted in the June 1944 issue, page 233.

44-117—Capacitor Installation at Newport News; *V. R. Parrack (M'37), E. L. Harder (M'41).* Abstracted in the June 1944 issue, page 233.

44-118—Capacitors, Condensers, and System Stability; *J. W. Butler (M'38), T. W. Schroeder (M'44), W. Ridgway (A'41).* Abstracted in the June 1944 issue, page 233.

44-130—Impulse Strength of Insulated-Power-Cable Circuits; *Herman Halperin (M'26), G. B. Shanklin (M'29).* Abstracted in the June issue, page 233.

44-131—Cyclic Movement of Cable—Its Causes and Effects on Cable-Sheath Life; *C. S. Schifreen (M'43).* Abstracted in the June 1944 issue, page 233.

44-215—An Analysis to Determine the Optimum Bussing Arrangements and Transmission Capabilities at Grand Coulee; *B. V. Hoard (M'36), G. W. Bills (A'38).* Abstracted in the August 1944 issue, page 314.

44-226—New 138-Kv Underground Cable Lines in Los Angeles; *C. G. Mansfield (M'42).* Abstracted in the August 1944 issue, page 314.

AIEE Board of Directors Meets

At the regular meeting of the AIEE board of directors held at Institute headquarters, New York, N. Y., November 2, 1944, the board confirmed approval by the executive committee, for submission to the American Standards Association for adoption as an American Standard, a proposed revision of American Standard Z32.5, "Graphical Symbols for Telephone, Telegraph, and Radio Use."

Also confirmed were the following actions of the executive committee as of September 27, 1944: 12 applicants transferred to the grade of Fellow; 89 applicants transferred, and 57 elected to the grade of Member; 219 applicants elected to the grade of Associate; 421 Students enrolled.

Reports were presented of meetings of the board of examiners held September 21 and 29 and October 19, 1944, and the recommendations adopted at those meetings were approved by the board of directors. Upon recommendation of the board of examiners, the following actions were taken: 22 applicants were elected to the grade of Member; 74 applicants were elected, and one was reinstated to the grade of Associate; 200 Students were enrolled.

Upon petition, the members of the board present voted unanimously for the election of Dugald C. Jackson as an Honorary Member of the Institute, the votes of absent members to be obtained by letter ballot, as the constitution specifies that Honorary Members shall be elected by the unanimous vote of the entire board. (Affirmative votes subsequently were received from all absent board members.)

The finance committee reported budget expenditures amounting to $29,144.76 in August, $28,895.93 in September, and $38,683.02 in October and submitted a budget for the appropriation year beginnning October 1, 1944, amounting to $450,000, which was approved.

A new formula, increasing the allotment of funds to Sections, as recommended by the Sections committee and the finance committee and applied to the 1944–45 budget, was adopted as a permanent policy, the Institute bylaws to be amended accordingly.

Approval was given to the dates, April 25–26, for the previously authorized North Eastern District technical meeting in Buffalo, N. Y., in 1945.

Plans of the 1945 winter technical meeting committee were reported and approved, including a joint evening session with the Institute of Radio Engineers, a smoker, and possibly technical inspection trips if they can be arranged.

Authorization was given for a Pacific Coast technical meeting in Seattle, Wash., in August 1945, the exact dates to be decided later.

Upon recommendation of the Standards committee, W. P. Dobson was appointed AIEE representative on the Joint Committee for Development of Statistical Applications in Engineering and Manufacturing.

In response to a request of the Mexico Section, the board, upon recommendation of the committee on planning and co-ordination, voted to grant to the Mexico Section financial assistance for an occasional meeting of the nature of a District meeting.

P. L. Alger, R. T. Henry, T. G. LeClair, S. H. Mortensen, and C. W. Ricker were elected board members to serve on the national nominating committee, in accordance with the bylaws. J. F. Fairman and N. E. Funk were designated as alternates.

The board confirmed the reappointment by the president of H. S. Osborne as an AIEE representative on the Standards council of the American Standards Association for the three-year term beginning January 1, 1945. H. E. Farrer, H. L. Huber,

and E. B. Paxton were reappointed alternates on the Standards council for the calendar year 1945.

The reappointment by the president of J. C. Parker as AIEE representative on the National Technical Civil Protection Committee for the administrative year 1944–45 was confirmed.

G. T. Harness having declined reappointment as the Institute's representative this year on the Radio Technical Planning Board, H. A. Affel, chairman of the committee on communication, was appointed in his place.

W. C. White, chairman of the committee on electronics, was appointed chairman of the special committee advisory to the AIEE representative on the Radio Technical Planning Board, in place of G. T. Harness, who declined reappointment this year.

Minutes of the meeting of the board of directors held August 2, 1944, were approved.

The board approved a letter sent jointly to Secretary of State Hull by the presidents of the four Founder Societies and the American Institute of Chemical Engineers, transmitting a suggested program for industrial control of postwar Germany, resulting from a study of the technical and industrial problems involved in the permanent disarmament. This plan pointed out the fact that an indiscriminate destruction of the German industrial system, failing to differentiate between the wartime and peacetime economy of the country, would penalize not only the owners of the materials destroyed, but the world as a whole, and outlined certain steps which, if followed, would make it impossible for Germany to wage or prepare for war, and would do this with the least disturbance to the normal economy of Western Europe.

Other subjects were discussed, reference to which may appear in this or future issues of *Electrical Engineering*.

Those present at the meeting were:

President—C. A. Powel, East Pittsburgh, Pa.

Past President—N. E. Funk, Philadelphia, Pa.

Vice-Presidents—L. A. Bingham, Boulder, Colo.; C. B. Carpenter, Portland, Oreg.; M. S. Coover, Ames, Iowa; J. F. Fairman, New York, N. Y.; W. J. Gilson, Toronto, Ontario, Canada; R. T. Henry, Buffalo, N. Y.

Directors—P. L. Alger, Schenectady, N. Y.; K. L. Hansen, Milwaukee, Wis.; C. M. Laffoon, East Pittsburgh, Pa.; T. G. LeClair, Chicago, Ill.; M. J. McHenry, Toronto, Ontario, Canada; C. W. Mier, Dallas, Tex.; S. H. Mortensen, Milwaukee, Wis.; W. B. Morton, Philadelphia, Pa.; D. A. Quarles, New York, N. Y.; W. R. Smith, Newark, N. J.

National Treasurer—W. I. Slichter, New York, N. Y.

National Secretary—H. H. Henline, New York, N. Y.

Industrial Control Apparatus. The "American Standard for Industrial Control Apparatus, *C*19.1" (AIEE number 15) issued in March 1944 should be corrected in accordance with the following errata tabulation:

Paragraph 15-101—Should read "Contactor ratings . . ."

Paragraph 15-109—Drop words "and numbers"

Bottom page 15—Equation should be $I_1 \times P^2 + 0.251_m$

Table on page 16—"Medium Duty" should read "65–80"

Paragraph 15-126—Rise for busses, straps, ... etc. should be "50C"

Paragraph 15-132—Equation should be

$$T = \frac{R}{r}(234.5 + t) - 234.5$$

Paragraph 15-146*a*—Should read "Watertight enclosures shall . . . . etc."

Future AIEE Meetings

Winter Technical Meeting
New York, N. Y., January 22–26, 1945

North Eastern District Meeting
Buffalo, N. Y., April 25–26, 1945

Summer Technical Meeting
Detroit, Mich., June 25–29, 1945

SECTION • • • • •

60th Anniversary of First Meeting Celebrated in Philadelphia

The atmosphere of the first recorded meeting of the AIEE held in Philadelphia, Pa., in October 1884 was recalled to an audience of members and guests of the Philadelphia Section and distinguished members of the Institute at a commemorative meeting held on the 60th anniversary of that first historic meeting.

The setting and historical significance of the first meeting was interpreted by National Secretary H. H. Henline (F '43) who followed the outline of the speech delivered on that occasion by Edwin J. Houston, associate of Elihu Thomson and Thomas Edison, who in 1884 was instructor in the Central High School in Philadelphia. Mr. Henline added a note of authenticity to his address by frequent quotations from the minutes of the first meeting and from Professor Houston's original talk on the phenomenon known as the "Edison effect."

Charles A. Powel (F '41), AIEE president, devoted his speech on "Romance in Engineering" to inspiring in his hearers the pioneer's attitude toward improvements on what may seem to be finished products and to exploding the assumption of the casual observer that all the nooks and crannies of electrical science have been explored. As an illustration Mr. Powel pointed out that the modern locomotive is as much an advance over the first locomotive as the first locomotive was over any previous mode of locomotion. New vistas are opening continually—some of them to be found through obvious and routine tasks, others through achievements requiring keen analytical insight and aggressive work, said Mr. Powel.

In his discussion of "Electronics—From Glow to Glamour," W. C. White (M '30), director of the electronics laboratory of the General Electric Company returned to the "Edison effect" of 60 years ago and traced its development down to present-day electronics. Two applications of particular interest described by Mr. White were the magnetic-wire recorder and a modern design of a little publicized lighthouse tube. The ease and simplicity of operation of the recorder was demonstrated by an on-the-spot recording of Mr. White's talk.

Two past presidents of the AIEE, Nevin E. Funk (F '34) and David C. Prince (F '26) also were among the record audience of 230. A dinner which preceded the meeting was attended by 130 persons.

ABSTRACTS • • •

TECHNICAL PAPERS previewed in this section will be presented at the AIEE winter technical meeting, New York, N. Y., January 22–26, 1945, and will be distributed in advance pamphlet form as soon as they become available. Copies may be obtained by mail from the AIEE order department, 33 West 39th Street, New York 18, N. Y., at prices indicated with the abstract; or at five cents less per copy if purchased at AIEE headquarters or at the meeting registration desk.

Mail orders will be filled AS PAMPHLETS BECOME AVAILABLE

Air Transportation

45-3—Speed-Control System for an Aircraft-Cabin-Supercharger Drive; *F. W. Godsey (M '36), J. D. Miner, Jr. (M '42), O. C. Walley (A '44). 15 cents.* Cabin supercharging is becoming extremely important as ceilings for military aircraft are extended upward. Many different schemes have been worked out for using main-engine power to drive variable-speed blowers at speeds which will maintain the desired airflow and cabin pressure at various altitudes. Two fundamental problems are:

1. Providing a variable-ratio transmission between the engine and the blower.
2. Controlling the blower speed to maintain the required air delivery.

In some cases a third problem arises because the point of air delivery is at a considerable distance from the engine, necessitating either a long transmission of power or the use of long air ducts. Requirements for remote supercharger drives of moderate power may be met by means of a variable voltage electric coupling. By using suitable motor characteristics, the air delivery of a blower connected to such an electric coupling can be controlled by a simple regulator designed to vary current in proportion to voltage. Requirements for the electric equipment are developed and laboratory tests on a nine-horsepower model are presented to demonstrate that the equipment meets the specified performance.

45-5—Simulated High-Altitude Testing of Aircraft Ignition Cables and Connectors; *H. H. Race (F '39), A. M. Ross, Jr. 15 cents.* A simulated-high-altitude test apparatus has been constructed for subjecting shielded ignition-cable-connector spark-plug assemblies to high voltage, high temperature, and reduced pressure, the two latter variables being cycled to reproduce an essential and severe aspect of aircraft-engine operation. Several hundred assemblies of many types have been tested in this way, and the results on test life indicate conclusively the inferiority of conventional lacquered-braid ignition cables and the necessity for positive and permanent sealing of the spark-plug connector. Two components developed during the period have been tested exhaustively and proved to increase test-life substantially:

1. Neoprene-sheathed ignition cables.
2. A composite neoprene–Mycalex integrally molded cable terminal.

Although it does not supplant regular acceptance or manufacturing control testing of ignition cables, the simulated-high-altitude tester fills a definite need for obtaining information on new cables and connectors in advance of engine tests.

45-6—Aircraft Illumination; *W. W. Davies. 15 cents.* This paper discusses generally the desirability of a complete redesign of all lighting of pilot compartments; of sealed beam-type landing lights; of such changes in landing lights as automatic bulb changers similar to those used in airway beacons; of more complete lighting of individual airport runways; of eliminating the flashing lights on the belly and top of the plane; and of the flashing tip and tail lights so as to give adequate visibility from all angles to approaching aircraft. With pilot-compartment lighting, it is pointed out that there is a general need for over-all illumination of the cockpit primarily for use in checking controls and instruments prior to take off and after landing as well as for use in completing paper work in the cockpit. Instrument lighting for en route flying, however, is extremely important, and it is essential to have all instruments lighted to the same degree with a provision for changing the intensity by means of a suitable control. Use of special lighting as well as improved over-all cockpit lighting, such as fluorescent lights, spot sources of ultraviolet for lighting charts and records, so-called "red light" for the illumination of instruments, and other such developments. The sealed-beam-type landing light has been suggested to the Civil Aeronautics Authority for further study. It also is pointed out that increased operations, both in terms of volume and number of aircraft, along with the possibility of improved landing equipment permitting operation in more inclement weather would make the "ribbon lighting" of runways desirable. Elimination of belly and top flashing lights on aircraft is recommended in view of reflections in the cockpit of the airplane from these lights in mist or overcast conditions. Improved methods for the installation of reading lights in the airplane cabin, for lights on loading and unloading equipment for both passengers and cargo, and for aircraft servicing lights also are considered and advantages and disadvantages discussed.

Automatic Stations

45-7—Supervisory Control for New Chicago Subway; *W. A. Derr (A'43), C. J. Buck, J. A. Stoos. 15 cents.* Visicode supervisory control is used to control the power supply to the 4.9 miles of subway in Chicago, which were put into operation in October 1943. In addition to controlling and supervising circuit breakers, the supervisory equipment also controls and supervises the position of ventilating fans throughout the subway and supervises the position of other devices. The supervisory-control dispatching-office equipment is located on the 12th floor of the Edison Building. The remote-station supervisory-control equipment is located at eight control centers in the subway.

Because of the scattered location of equipment throughout the subway, special single- and two-wire extensions were constructed from the control centers to provide control and supervision of these devices. The scheme used provides continuous supervision of the continuity of the direct-wire extension circuits. On the automatic-reclosing feeder breakers a new arrangement of supervision was provided. In addition to the standard red and green lamp indications, an amber lamp also is used. The red lamp indicates that the breaker is closed; the green lamp indicates that the breaker is tripped and locked out; and the amber lamp indicates that the breaker is tripped but not locked out. The extra lamp was provided to prevent the possibility of having a breaker automatically reclose while men were working on a faulted section. On all supervisory-control systems built in the past, the change in position of a device always has been indicated by the lighting of a disagreement lamp. The subway installation marked the advent of a new type of indication to indicate an automatic operation—the flashing of the red, green, or amber position-indicating lamp. The flashing-lamp signal has proved to be a definite improvement in locating a unit which has changed position. The compact arrangement of the dispatching-office control equipment places the entire control at the finger tips of a single operator.

Basic Sciences

45-1—Two-Phase Co-ordinates of a Four-Phase Network; *E. W. Kimbark (M'35). 15 cents.* A four-phase five-wire network is analyzed by regarding it as two single-phase three-wire networks with common neutral and then by replacing each of these two networks by their symmetrical-component networks of zero and first order. The resulting substitute quantities are called the two-phase co-ordinates of the four-phase network. Their properties are similar to those of two-phase co-ordinates of a three-phase network. The two first-order networks have voltages and currents which are normally equal in magnitude and 90 degrees apart in phase and which thus constitute a two-phase system. The four substitute networks are independent, if the original four-phase network is symmetrical with respect to two perpendicular axes. Connections between substitute networks for representing 16 types of short circuit on a four-phase network are given both for symmetrical components and for two-phase co-ordinates.

Power Generation

45-4—Evaluation of Electric Distribution Losses in Terms of Generating-Station Capacity; *M. Mortara. 15 cents.* Generally accepted methods are available for estimating the economic value of the energy absorbed by electric losses in a distribution system. There is no consensus however, on the economic evaluation of these losses in terms of their influence upon generating-station capacity requirements. Such evaluation for an electric system of given characteristics can be made by mathematical analysis on the basis of the long-range trend curve, representing the expected development of the system. With appropriate assumptions on the type of curve to be used, a rational method is developed for estimating the deferments in the requirements for new generating capacity obtainable by reduction of power losses. The economic value of such deferments then is calculated, showing that the cost of power losses is comparable with, and may be considerably greater than, that of the consequent energy losses. That factor, therefore, never should be neglected in planning studies.

45-9—Use of Dielectric-Absorption Tests in Drying Out Large Generators; *H. C. Marcroft (A'31). 15 cents.* Dielectric absorption is the name commonly given to the effect which takes place in electrical insulation, when it is subjected to a direct potential, with reference particularly to the manner in which the resulting charging current into the insulation decreases with time. The time rate of change and the magnitude of the observed current are indications of the amount of absorbed moisture (among other things) existing in the insulation of large generators, which apparatus affords a practical application of the dielectric-absorption phenomenon. A newly wound generator or a generator long idle contains moisture in the insulation, which should be removed before full operating voltage is applied to the winding insulation. The method of using the dielectric-absorption effect to determine the state of dryness of large generators is presented as a new means of measurement and one better adapted to giving more information than methods previously used.

Marine Transportation

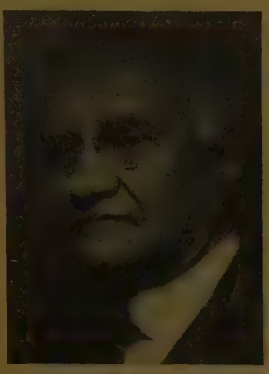
45-2—Damping Effect of D-C Marine Propulsion Motors on Vibrations Produced in Drive Shafts by Large Propellers; *Bernard Litman (A'42). 15 cents.* D-c machines often are employed in ship-propulsion drives where maneuverability and a wide speed range are required. The d-c machine also has the advantage of serving as a source of damping, if propeller pulsation should coincide with the natural frequency at some speed. This damping force arises because of the fact that any change in speed of the motor will cause a corresponding change in load current and torque. This torque change acts to oppose the original speed change, thus providing an effective damping force. The necessary equations and circuits are developed for calculating this damping effect, and an illustrative calculation is included with actual test values for comparison.

Protective Devices

45-8—Graphical Method of Calculating Fault Currents on Rural Distribution Systems; *F. W. Linder (A'41). 15 cents.* The purpose of this paper is to describe a simple graphical method of calculating fault currents on distribution systems which gives accurate results and saves time and effort in making the calculations. Special reference is given to rural distribution systems of the multigrounded-neutral type; however, the principles of the graphic method may be applied to any system. Fault currents are calculated on current diagrams which are easy to construct on standard rectangular co-ordinate graph paper. The current diagrams are designed for a specific system voltage, and a separate diagram is used for each type of fault. When the fault currents on a system have been calculated by the graphical method, the current diagrams will aid in system operation, because they give a visual picture of the possible fault currents at any location on the system.

PERSONAL ....

Dugald C. Jackson (A '87, M '90, F '12) professor emeritus of electrical engineering, Massachusetts Institute of Technology, Cambridge, was elected an Honorary Member of the Institute on November 2, 1944. He was born in Kennett Square, Pa., on February 13, 1865, and received a degree in civil engineering from Pennsylvania State College in 1885. He also has been awarded an honorary doctor-of-science degree by Columbia University and an honorary doctor-of-engineering degree by Northeastern University. Doctor Jackson began his career in



Dugald C. Jackson

1887 as vice-president of the Western Engineering Company, Lincoln, Neb. In 1889 he joined the Sprague Electric Railway and Motor Company, New York, N. Y., as assistant chief engineer, and in 1890 he became a chief engineer for the Edison Electric Company. In 1891, with his brother, W. B. Jackson, Doctor Jackson organized a firm of consulting engineers and also was appointed professor of electrical engineering at the University of Wisconsin, Madison. He maintained this position until 1907 when he joined the staff of the Massachusetts Institute of Technology as professor and head of the department of engineering. Meanwhile, in 1919, he helped to organize the firm of Jackson and Moreland and remained as senior partner until 1930. He retired from the Massachusetts Institute of Technology as professor emeritus in 1935. Doctor Jackson has been a member and chairman of many Institute committees, including the Standards committee, the transportation committee, and the Edison Medal committee, and has served as vice-president (1897–99) and president (1910–11) of AIEE. During the first World War he served as a lieutenant colonel with the United States Army in France. He was a Lamme Medalist in 1931 and received the Edison Medal in 1938; is a member of the American Society of Mechanical Engineers, the American Society of Civil Engineers, the Institution of Electrical Engineering (London, England), the Société Française des Electriciens (Paris, France), the American Institute of Consulting Engineers, and the Engineers' Council for Professional Development; and has written several books and many articles on the subject of electrical engineering.

W. E. Everitt (A '25, F '36) director of operational research, Office of the Chief Signal Officer, United States War Department, Washington, D. C., has been appointed professor and head of the department of electrical engineering, the University of Illinois, Urbana. His duties will begin immediately upon his release from war service. Doctor Everitt was graduated from Cornell University, Ithaca, N. Y., in 1922. In 1926 he received the degree of master of science from the University of Michigan, Ann Arbor, and in 1933 he was awarded the degree of doctor of philosophy by Ohio State University, Columbus. From 1920 until 1922 he taught electrical engineering at Cornell University and after his graduation was employed by the North Electric Manufacturing Company, Galion, Ohio, as engineer in charge of the design and development of private telephone exchanges. Doctor Everitt joined the staff of the University of Michigan as an instructor in electrical engineering in 1924 and remained at that institution until 1926 when he was appointed assistant professor of electrical engineering at Ohio State University. He became associate professor in 1929 and professor in charge of communication courses in 1934 but was sent to Washington in 1942 to direct research for the Signal Corps. Doctor Everitt was recently elected president of the Institute of Radio Engineers.

G. M. Keenan (F '36) formerly general sales manager, Pennsylvania Power and Light Company, Allentown, has been appointed chief engineer. Mr. Keenan entered the engineering field as a student apprentice with the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., after his graduation from Purdue University in 1913. In 1915 he went to work for the Merchants' Heat and Light Company, Indianapolis, Ind., as repairman, subsequently rising to the position of superintendent of power, but he returned to the Westinghouse Company in 1916. In 1917 he became a test engineer, the Union Gas and Electric Company, St. Louis, Mo., and then superintendent of power, Little Rock (Ark.) Railway and Electric Company. Mr. Keenan became associated with the Lehigh Navigation Electric Company, Allentown, Pa., which was merged with the Pennsylvania Power and Light Company, in 1918. He assumes his duties as chief engineer after more than two years with the War Production Board, Washington, D. C., in connection with electric-power supply.

J. T. Kimball (A '18) electrical engineer, Niagara Hudson Power Corporation, Buffalo, N. Y., has been elected a vice-president and director of the New York Power and Light Corporation and a vice-president of the Central New York Power Corporation, Syracuse. He will continue to act as consultant for the companies in the western division of the Niagara Hudson system. Mr. Kimball was graduated from the University of Wisconsin in 1914 and then entered the employ of the Bell Telephone Company, Chicago, Ill. From 1916 until 1918 he was on the engineering staff of the Wisconsin Railroad Commission, Madison, and in 1918 he entered the firm of Ford, Bacon, and Davis, consulting engineers, New York, N. Y. He stayed with this company until 1932 when he became a rate and valuation engineer for the Niagara Hudson Power Corporation.

O. B. Blackwell (A '08, F '17) formerly vice-president, Bell Telephone Laboratories, Inc., New York, N. Y., has been elected to the board of directors of that company and on October 1, 1944, was appointed an assistant vice-president, American Telephone and Telegraph Company, New York. Mr. Blackwell was graduated from the Massachusetts Institute of Technology in 1906 and in the same year entered the engineering department of the American Telephone and Telegraph Company, becoming transmission and protection engineer in 1914. In 1919 he was made transmission development engineer, and when that department was transferred to the Bell Telephone Laboratories in 1934 he became director of transmission development. Mr. Blackwell was appointed manager of staff departments in 1935 and in 1936 was elected vice-president.

J. B. Harris, Jr. (A '17) vice-president, Rumsey Electric Company, Philadelphia, Pa., has been elected president of that company. After his graduation from the Bliss Electrical School in 1911, Mr. Harris was connected with the General Electric Company and the Westinghouse Electric and Manufacturing Company. He was employed as sales engineer by the Pittsburgh (Pa.) Transformer Company in 1917 and in 1920 was a member of the firm of Harris and Evans, Philadelphia, Pa., which became the firm of Harris and Butler in 1923. Mr. Harris was made president of the latter firm in 1925 and continued in that position until 1935 when he was appointed manager of the electrical-equipment department, Rumsey Electric Company. He became vice-president in 1939.

Deveraux Martin (M '43) formerly chief radio engineer, the J. H. Bunnell Company, Brooklyn, N. Y., has been appointed chief engineer, the Wilcox-Gay Corporation, Charlotte, Mich. Mr. Martin is a graduate of the Massachusetts Institute of Technology (1929). He was with the Westinghouse Electric and Manufacturing Company, Chicopee Falls, Mass., from 1929 until 1931 as development engineer, and then he entered the employ of the De Forest Radio Company, Passaic, N. J. After a period of five years during which he managed his own business, the Martin Radio Laboratory, Newcastle, Me., Mr. Martin was employed by the Radio Receptor Company, New York, N. Y. (1937) and the Federal Telegraph Company, Newark, N. J. He joined the J. H. Bunnell Company in 1942.

J. S. Morgan (A '39) switchgear division, Allis-Chalmers Manufacturing Company, Milwaukee, Wis., has been appointed sales engineer-in-charge of switchgear sales. **W. M. Pickslay** (A '31, M '40) assistant engineer, has been appointed assistant engineer-in-charge of the control section of the electrical department of the Allis-Chalmers Company.

OBITUARY

Frank Anderson Merrick (A '07) vice-chairman of the board of directors, Westinghouse Electric and Manufacturing Company, Pittsburgh, Pa., died October 26, 1944. Mr. Merrick was born in Pennsylvania in 1868 and was graduated from Lehigh University in 1891. After his graduation he became an apprentice at the Thomson-Houston Electric Company, Lynn, Mass., and by 1892 had advanced to factory construction inspection. He joined the firm of Blood and Hale, consulting engineers, Boston, Mass., in 1897 and in 1898 became manager and chief engineer of the Steel Motor Company, Johnstown, Pa., a subsidiary of the Lorain Steel Company. When the Westinghouse Electric and Manufacturing Company acquired the electrical branch of the company, Mr. Merrick was transferred to the Westinghouse plant at East Pittsburgh, Pa. In 1903 he supervised the building of the plant at Hamilton, Ontario, Canada, and became its manager of works, progressing to vice-president and general manager of the Canadian Westinghouse Company, Ltd. During the first World War he took charge of the Chicopee Falls, Mass., factory and after the war was sent to England as special representative of the Westinghouse Electric International Company. In 1921 he reassumed his duties in Hamilton, Ontario, and in 1925 he was made vice-president and general manager of the company's offices at East Pittsburgh. Mr. Merrick became president of the company in 1929 and vice-chairman of the board in 1938.

John H. Gardner (A '25, M '34) brigadier general, United States Army, Washington, D. C., died October 11, 1944. General Gardner was born in Meadowdale, N. Y., on October 10, 1893, and was a graduate of Union College with a bachelor-of-science degree. He also held a master-of-science degree in electrical engineering from Union College (1915) and a master-of-science degree in communication engineering from Yale University (1925). General Gardner was associated with the General Electric Company in Schenectady, N. Y., and Chicago, Ill., from 1913 to 1916. When the United States entered the first World War in 1917 he was commissioned a second lieutenant in the Army and was with the American forces in occupied Germany until 1920. In 1921 he returned to the United States as a captain in the Signal Corps, and in 1943 he was promoted to the rank of brigadier general with command of the Signal Corps Aircraft Signal Service, Wright Field, Dayton, Ohio. General Gardner was transferred to Washington in August 1943 as assistant chief of the Procurement and Distribution Service.

Norman Short Braden (A '09, M '20) vice-chairman of the board, Canadian Westinghouse Company, Ltd., Hamilton, Ontario, Canada, died September 27, 1944. Mr. Braden was born on June 15, 1869, in Indianapolis, Indiana. His first position was as an apprentice in the shop of the Jenney Electric Company in 1892, and in 1894 he became construction and sales engineer. In 1899 he joined the Westinghouse Electric and Manufacturing Company, Cleveland, Ohio, as sales engineer, rising to district manager in 1903. He was transferred to the Canadian Westinghouse Company in 1904 as manager of the sales department, and was appointed third vice-president in 1919, member of the board of directors in 1926, and vice-chairman of the board of directors in 1939. Mr. Braden retired from active service this year but remained vice-chairman. He was a member of the Engineering Institute of Canada.

Cornelius Edmond O'Donnell (A '32) field supervisor, operating department, Central Illinois Public Service Company, Mattoon, died October 3, 1944. Born in Rantoul, Ill., on September 10, 1899, Mr. O'Donnell was graduated from the University of Illinois in 1930 with the degree of bachelor of science in electrical engineering. In July of the same year he started the student engineering course of the Westinghouse Electric and Manufacturing Company, in October he went to work on the test floor of the company at East Pittsburgh, Pa., and in 1931 he entered the supply engineering department laboratory. Mr. O'Donnell joined the Central Illinois Public Service Company in 1935 as distribution clerk and was made field supervisor in 1940.

Louis Washington Pratt (A '38) assistant engineer, municipal department, Hydro-Electric Power Commission of Ontario, Toronto, Canada, died October 3, 1944. Mr. Pratt was born in London, England, on April 25, 1877. In 1896 he was employed by the Brantford (Ontario) Electric and Operating Company, Ltd. Subsequently, he was with the Western Counties Electric Company, Ltd., Brantford (1905), and the Federal Electrical Construction Company, Ltd., Toronto (1906). In 1907 Mr. Pratt joined the Dominion Power and Transmission Company, Ltd., Hamilton, Ontario, where he stayed until 1932 when he entered the employ of the Hydro-Electric Power Commission.

Henry Escher, Jr. (A '37) lieutenant in the Armed Forces of the United States and formerly of the industrial department, aviation division, General Electric Company, Schenectady, N. Y., was killed in action in June 16, 1944. Lieutenant Escher was born on July 1, 1913, in Brooklyn, N. Y., and is a graduate of Princeton University, Princeton, N. J., (1934). From 1934 to 1936 he was an assistant instructor at Princeton University, where he taught laboratory classes in introductory electrical engineering. He joined the General Electric Company, Bloomfield, N. J., as a student engineer in 1936 and was employed in the Schenectady plant when he entered the armed services this year.

Ernest Oswald Stockman (A '39) lieutenant commander, Royal Canadian Naval Volunteers Reserve, formerly resident naval engineer overseer, Halifax Shipyards, Dartmouth, Nova Scotia, Canada, has been reported missing in action. Born in Newcastle-on-Tyne, England, on May 28, 1908, Commander Stockman attended Rutherford College. He was employed by C. A. Parsons and Company, Ltd., Newcastle-on-Tyne in various engineering capacities from 1923 until 1934 when he became sales engineer in the Canadian office of the C. A. Parsons Company, Toronto, Ontario. Commander Stockman received his commission in the RCNVR in 1941.

Carlyle J. Roberts (A '22) chief inspector, Leeds and Northrup Company, Philadelphia, Pa., died June 26, 1944. Mr. Roberts was born on January 26, 1897, in Granville, Ohio. He was a graduate of Denison University with a bachelor-of-science degree in physics and has been with the Leeds and Northrup Company since 1918 when he was placed in charge of laboratory standards and equipment.

Leo Behr (A '29) research department, Leeds and Northrup Company, Philadelphia, Pa., died June 25, 1944. He was born in New York, N. Y., in 1898 and was the recipient of both the degree of mechanical engineering and the degree of doctor of philosophy from Cornell University. Doctor Behr became associated with the Leeds and Northrup Company in 1917 in the development department.

MEMBERSHIP

Recommended for Transfer

The board of examiners, at its meeting on November 16, 1944, recommended the following members for transfer to the grade of membership indicated. Any objections to these transfers should be filed at once with the national secretary.

To Grade of Fellow

Danner, R. F., general supt., Oklahoma Gas & Elec. Co., Oklahoma City, Okla.
Gross, I. W., electrical research engr., American Gas & Electric Service Corp., New York

2 to grade of Fellow

To Grade of Member

Askey, J. S., section engr., Westinghouse Elec. & Mfg. Co., E. Pittsburgh, Pa.
Brownlee, T., lightning arrester research & development, General Elec. Co., Pittsfield, Mass.
Chisholm, H. E., asst. engr. of equipment, Bell Tel. Co. of Pa., Pittsburgh, Pa.
Church, E. A., engr., Boston Edison Co., Boston, Mass.
Crandell, C. F., member, technical staff, Bell Tel. Labs., New York
Crawford, C. S., plant engr., Charity Hospital of Louisiana, New Orleans, La.
Eardley, M. V., senior elec. engr., Dept. of Water & Power, Los Angeles, Calif.
Freundt, G. L., elec. engr., Sievert Elec. Co. Inc., Chicago, Ill.
Gille, W. H., chief engr., Aero Div., Minneapolis-Honeywell Regulator Co., Minneapolis, Minn.
Goodale, E. C., elec. engr., Bonneville Power Adm., Portland, Ore.
Harvey, R. H., general mgr. and chief engr., Companhia Carris de Ferro de Lisboa, Lisboa, Portugal
Kahn, Frank, senior technical asst., Philadelphia Elec. Co., Philadelphia, Pa.
Kap, F. W., elec. engr., Sanitary Dist. of Chicago, Chicago, Ill.
Keever, L. M., associate prof. of E.E., North Carolina State College, Raleigh, N. C.
Meier, Otto, asst. prof. of E.E., Duke Univ., Durham, N. C.
Moore, Paul, assoc. elec. engr., Public Works Dept., Navy Yard, Mare Island, Calif.
Mostrom, John, elec. engr., Phillips Petroleum Co., Bartlesville, Okla.
Parker, R. S., industrial engr., War Dept., Huntsville, Ala.
Payne, R. B., senior engr., War Production Board, Washington, D. C.
Quinlan, A. L., engr., Western Elec. Co., Chicago, Ill.
Rhoton, W. O., elec. engr., Delco-Remy Div., General Motors Corp., Anderson, Ind.
Schille, C. A., head, elec. engr., Industrial Engg. Div., Giffels & Vallet, Inc., Detroit, Mich.
Sigman, F. I., chief engr., Buffalo Elec. Co. Inc., Buffalo, N. Y.
Spaulding, A. M., plant assignment supervisor, Mountain States Tel. & Tel. Co., Denver, Colo.
Svendsen, G. P., president & treasurer, Boustead Elec. & Mfg. Co., Minneapolis, Minn.

Thomas, E. U., elec. engr., Specialties, Inc., Locust Valley, L. I.
Thomas, E. J., engr., General Elec. Co., Fort Wayne, Ind.
Walsh, F. R., president, Electric Switchboard Co., New York
White, J. E., engr., Westinghouse E. & M. Co., Bloomfield, N. J.
Williams, L. T., acting chief engr., Southwestern Gas & Elec. Co., Shreveport, La.

30 to grade of Member

Applications for Election

Applications have been received at headquarters from the following candidates for election to membership in the Institute. Any member objecting to the election of any of these candidates should so inform the national secretary before December 30, 1944, or February 28, 1945, if the applicant resides outside of the United States or Canada.

To Grade of Member

Adams, E. C., Gulf States Util. Co., Beaumont, Texas
Calvo, D., E., Nat. Univ. of Colombia, Bogota, Col., S. A.
Cavagnet, L. J., Cons. Tel. & Elect. Subway Co., New York, N. Y.
Cole, I. V. (Reelection), Lexington Elec. Prod. Co., Newark, N. J.
Crosman, W. T., Duquesne Lt. Co., Pittsburgh, Pa.
Dudrear, A. C. (Reelection), I. B. Abel & Son, York, Pa.
Fife, W. H. (Reelection), Austin Engg. Co., East Cleveland, Ohio
Freeman, N. L. (Reelection), Amer. Loco. Co., Schenectady, N. Y.
Gauggel, H. J. W., Gen. Elec. Co., Atlanta, Ga.
Gelder, W. A. E., Elect. Lt. Cmdr., R. N. V. R., Grimsby, England
Gunckel, W. J., Rural Elect. Admin., St. Louis, Mo.
Hannah, S. W., Rocky Mountain Arsenal, Denver, Colo.
Heffelman, M. C. (Reelection), N. Mex. Pr. Co., Santa Fe, N. Mex.
Heinz, W. B., Cons. Engr., Bound Brook, N. J.
Huppert, W. (Reelection), H. Goodman & Sons, Inc., New York, N. Y.
Hutchinson, C. C., Engg. Labs., Inc., Tulsa, Okla.
James, A. J. T., Hackbridge Elec. Const. Co., Ltd., Walton-on-Thames, England
Kelly, H. A., Wheeling Stamping Co., Wheeling, West Va.
Korn, F. A. (Reelection), Bell Tel. Lab., Inc., New York, N. Y.
Krueger, H. H. (Reelection), Utah Pr. & Lt. Co., Salt Lake City, Utah
Leonard, E. M. (Reelection), Allis-Chalmers Mfg. Co., Pittsburgh, Pa.
Lopez, J. J., Amer. Tel. & Tel. Co., Albuquerque, N. Mex.
Lovfald, P., Cons. Engr., New York, N. Y.
Mahoney, J. H., Boston Edison Co., Boston, Mass.
Markers, H. W. (Reelection), Commonwealth Edison Co., Chicago, Ill.
McCrocklin, A. J., Jr., Dow Chem. Co., Freeport, Tex.
Mead, S. N., Henschel Corp., Amesbury, Mass.
Miller, H. C., Alan Wood Steel Co., Conshohocken, Pa.
Mollo, G. A., Koppers Co., Inc., Pittsburgh, Pa.
Munro, J. C., B. C. Elec. Ry. Co., Ltd., Vancouver, B. C., Can.
Pennell, C. E., N. J. Bell Tel. Co., Newark, N. J.
Pentland, L., Combined Prod. & Res. Board, Washington, D. C.
Perry, A., Norwich Corp., Norwich, England
Persons, J. T. (Reelection), Cen. Pr. & Lt. Co., Corpus Christi, Texas
Post, J. V., Gulf States Util. Co., Navasota, Texas
Reisz, M. O., Elec. Zinc Co. of A'Asia, Ltd., Hobart, Tasmania
Rieman, E. F., Okonite Co., Passaic, N. J.
Schley, W. H., Dallas Pr. & Lt. Co., Dallas, Texas
Seaholm, W. E., Acting City Manager, Austin, Texas
Shanely, F. D., Lear, Inc., Piqua, Ohio
Steven-Hubbard, N., Cadet, OCTU, East Africa Command
Taylor, J. L., Mid-Lincolnshire Elec. Sup. Co., Ltd., Grantham, England
Turner, J. G. (Reelection), U. S. Bur. of Reclamation, Denver, Colo.
Walter, G. F. (Reelection), Pub. Serv. Elec. & Gas Co., Maplewood, N. J.
Wells, H. J., Stone & Webster, Oak Ridge, Tenn.
Witherspoon, J. (Reelection), Gen. Elec. Co., Schenectady, N. Y.
Zobel, A. E., Tenn. Eastman Corp., Oak Ridge, Tenn.

47 to grade of Member

To Grade of Associate

United States and Canada

1. Northeastern

Belluscio, F. J., Taylor Inst. Co., Rochester, N. Y.
Brown, H. A., Walsh Kaiser Co., Providence, R. I.
Brown, H. R., Jr., Gen. Elec. Co., Schenectady, N. Y.
Buck, F. W., United Illum. Co., New Haven, Conn.
Butz, A. N., Jr., Harv. Underwater Sound Lab., Cambridge, Mass.
Conway, G. J., Gen. Elec. Co., Buffalo, N. Y.
Cottrell, C. L., Cornell Univ., Ithaca, N. Y.
Dautrich, G. U., Gen. Elec. Co., Hartford, Conn.
Ellsworth, I. W., Westinghouse Elec. & Mfg. Co., Boston, Mass.
Farrar, R. O., Westinghouse Elec. & Mfg. Co., Boston, Mass.
Fisher, J. C., Harvard Engg. School, Cambridge, Mass.
Jenkins, W. C., Westinghouse Elec. & Mfg. Co., Syracuse, N. Y.
Kappauf, E. F., Ensign, USNR, Boston, Mass.
McHugh, H. R., Kennecott Wire & Cable Co., Phillipsdale, R. I.
Miller, J. C., N. Y. State Elec. & Gas Corp., Lockport, N. Y.
Waynick, A. H., Harvard Univ. Cambridge, Mass.
Weiss, H. E. (Reelection), Allis-Chalmers Mfg. Co., Buffalo, N. Y.
Wood, M. L., Curtiss-Wright Corp., Buffalo, N. Y.

2. Middle Eastern

Ackermann, R. W., Amer. Tel. & Tel. Co., Cleveland, Ohio
Andrews, D. E., Master Elec. Co., Dayton, Ohio
Attwood, S. H., Master Elec. Co., Dayton, Ohio
Bova, N. J., Master Elec. Co., Dayton, Ohio
Boyd, R. C., Westinghouse Elec. & Mfg. Co., East Pittsburgh, Pa.
Campbell, F. S., Hartford Steam Boiler Insp. & Ins. Co., Philadelphia, Pa.
Carle, J. H., Carle Elec. Const. Co., Akron, Ohio
Carver, D. W. (Reelection), Master Elec. Co., Dayton, Ohio.
Crockett, J. W., Graybar-Elec. Co., Inc., Baltimore, Md.
Davis, R. E., A. T. S. C., Wright Field, Dayton, Ohio
DuBroff, W. C., Captain, U. S. Army, Wright Field, Dayton, Ohio
George, A. B., Wilbur Watson & Assoc., Cleveland, Ohio
Gilchrist, A. D., Leece-Neville Co., Cleveland, Ohio
Gray, M. H., A.T.S.C., Wright Field, Dayton, Ohio
Hanson, F. L., Ideal Elec. & Mfg. Co., Mansfield, Ohio
Harris, M., Natl. Elec. Coil Co., Bluefield, W. Va.
Harry, J. N., Wright Field, Dayton, Ohio
Haynes, C. H., Bird Engg. Co., Cleveland, Ohio
Hertz, H. F. (Reelection), Phila. Elec. Co., Philadelphia, Pa.
Hughes, R. E., National Elec. Coil Co., Bluefield, W. Va.
Hunt, W. A., Radio Station WHIZ, Zanesville, Ohio
Kraus, R., Cudahy Packing Co., Philadelphia, Pa.
Lang, J. S., Auto. Temp. Cont. Co., Philadelphia, Pa.
Livermore, E. H., Jr., Del. Pr. & Lt. Co., Wilmington, Del.
MacCauley, T. H., Gen. Elec. Co., Philadelphia, Pa.
Nordfelt, H. C., Cont. Sales & Engg. Co., Pittsburgh, Pa.
Oppermann, R. H. (Reelection), Assoc. Fact. Mut. Fire Ins. Cos., Philadelphia, Pa.
Poeschl, J. J., Atlantic Ref. Co., Philadelphia, Pa.
Price, S., Electric Cont. & Mfg. Co., Cleveland, Ohio
Starbuck, H. S., Gen. Motors Corp., Dayton, Ohio
Strahle, P. J., Leece-Neville Co., Cleveland, Ohio
Taylor, L. M., Bureau of Ships, Washington, D. C.
Trautman, D. L., Jr., Carnegie Inst. of Tech., Pittsburgh, Pa.
Wolfe, K., Leece-Neville Co., Cleveland, Ohio
Wollerton, C. E., Gen. Elec. Co., Philadelphia, Pa.

3. New York City

Alden, A., York & Sawyer, New York, N. Y.
Backer, L. B., N. Y. Univ., New York, N. Y.
Bolles, H. J., Weston Elec. Inst. Corp., Newark, N. J.
Di Toro, M. J. (Reelection), Hazeltine Corp., Little Neck, N. Y.
Judkins, R. P., Lieut. (jg), USNR, New York, N. Y.
Lacher, K. K., Westinghouse Elec. & Mfg. Co., New York, N. Y.
Laestadius, J. E., Columbia Univ., New York, N. Y.
Larys, B., Diehl Mfg. Co., Finderne, N. J.
Marino, J. L., Radio Corp. of America, Harrison, N. J.
Martinez-Peralta, A., Fed. Tel. & Radio Corp., East Newark, N. J.
Maynard, M. J., Horni Signal Co., New York, N. Y.
Morse, H. D., George G. Sharp, New York, N. Y.
Murphy, F. A., Ward Leonard Elec. Co., New York, N. Y.
O'Brien, J. P., Anaconda Copper Min. Co., New York, N. Y.
Poehler, H. A., Int'l. Elec. Labs., New York, N. Y.
Quintana, C. H. M., Bell Tel. Lab., Inc., New York, N. Y.
Rick, F. L., Jersey Central Pr. & Lt. Co., Allenhurst, N. J.
Shumaker, H. A., Diehl Mfg. Co., Somerville, N. J.
Smith, F. T. (Reelection), Diehl Mfg. Co., Finderne, N. J.
Toole, L. J., Gen. Cable Corp., Bayonne, N. J.
Torcivia, J. A., Westinghouse Elec. & Mfg. Co., Newark, N. J.
Tracy, W. A., Bell Tel. Lab., Inc., New York, N. Y.
Whitman, H. M., Diehl Mfg. Co., Somerville, N. J.
Wieland, G. J., Jr., Bell Tel. Lab., Inc., New York, N. Y.

4. Southern

Balfour, H. B., Jr., La. Pr. & Lt. Co., West Monroe, La.
Becker, W. N., Clinton Engineer Works, Oak Ridge, Tenn.
Boch, A. L., Clinton Engineer Works, Oak Ridge, Tenn.
Cato, S. G., Memphis Lt. Gas & Water Div., Memphis, Tenn.
Drake, R.S., Chattanooga Elec. Pr. Board, Chattanooga, Tenn.
Greenberg, E. E. (Reelection), Kelley-Koett Mfg. Co., Covington, Ky.
Holland, J. T. (Reelection), Duke Pr. Co., Charlotte, N. C.
Mosso, A. J., Tennessee Eastman Corp., Oak Ridge, Tenn.
Sauer, C. M., Louis. Gas & Elec. Co., Louisville, Ky.
Smith, T. G., Allis-Chalmers Mfg. Co., New Orleans, La.
Thompson, E. H., Southern Bell Tel. & Tel. Co., New Orleans, La.
Walker, W. E., Gen. Elec. Co., Atlanta, Ga.

5. Great Lakes

Audier, M., Cutler-Hammer, Inc., Milwaukee, Wis.
Barta, W. J., Amer. Bur. of Shipping, Detroit, Mich.
Clement, M. A., Automatic Elec. Co., Chicago, Ill.
Dodd, C. M., Iowa State Col., Ames, Iowa
Glass, P., Askania Reg. Co., Chicago, Ill.
Harrison, D. L., Allen Bradley Co., Milwaukee, Wis.
Holmes, R. H., Minn. Pr. & Lt. Co., Duluth, Minn.
Jacobsen, T. H., Delta-Star Elec. Co., Chicago, Ill.
Januszko, E. J., Henry Ford Trade School, Dearborn, Mich.
Koerwitz, N. A., Board of Pub. Works, Niles, Mich.
Potter, L. B., Detroit Edison Co., Detroit, Mich.
Sancomb, C., Allis-Chalmers Mfg. Co., West Allis, Wis.
Schotz, A., Cutler-Hammer, Inc., Milwaukee, Wis.
Spellmire, G. W., Gen. Elec. Co., Chicago, Ill.
Stong, K. K., Allen-Bradley Co., Milwaukee, Wis.

6. North Central

Abrams, M. L., U. S. Bur. of Rec., Denver, Colo.

7. South West

Bartosh, F. A., Emerson Elec. Mfg., St. Louis, Mo.
Bates, J. L., Cen. Pr. & Lt. Co., Corpus Christi, Texas
Dugan, J. M., Gulf States Util. Co., Port Arthur, Texas
Figari, E. E., Gulf States Util. Co., Beaumont, Texas
Finley, R. A., Texas Elec. Serv. Co., Fort Worth, Texas
Groh, W. J., Cent. Kans. Elec. Coop. Assoc., Inc., Great Bend, Kans.
Lowery, J. M., Curtiss-Wright Corp., Robertson, Mo.
McCoppin, J. T., Gulf States Util. Co., Beaumont, Texas
McFarland, O. C. (Reelection), S. W. Bell Tel. Co., Kansas City, Mo.
Morton, R. E., Gulf States Util. Co., Navasota, Texas
Olive, W. W., Jr., Curtiss-Wright Corp., St. Louis, Mo.
Sims, V. R., Emerson Elec. Mfg. Co., St. Louis, Mo.
Walling, F. E., Cons. Steel Corp., Ltd., Orange, Texas
Walston, T. E., Jr., Cons. Steel Corp., Ltd., Orange, Texas
Wynn, T. C., Lower Colo. River Auth., Austin, Texas

8. Pacific

Cates, W. K., Westinghouse Elec. & Mfg. Co., Sacramento, Calif.
Catton, J. A., Lockheed Aircraft Corp., Burbank, Calif.
Chaika, S., Lieut. (jg), USNR, San Francisco, Calif.
Glazunov, S. V., C. C. Moore & Co., San Francisco, Calif.
Kemp, C. A., U. S. Navy Yard, Mare Island, Calif.
Larissou, J. P., A. W. Earl, Cons. Engr., San Francisco, Calif.
Main, H. E., North Amer. Avia., Inc., Inglewood, Calif.
Montroy, F. E., Montroy Elec. Mfg. Co., Los Angeles, Calif.
Stephens, H. R., South. Calif., Edison Co. Ltd., Chino, Calif.
Youngman, W. C., U. S. Navy Yard, Mare Island, Calif.

9. North West

Cerveny, J. P., Bonneville Pr. Adm., Portland, Oreg.
Hendrey, G. R., W. R. Hendrey Co., Seattle, Wash.
Howard, J. S., Alum. Co. of America, Trentwood, Wash.
Myrick, E. P., Mont. Pr. Co., Great Falls, Mont.
Palmer, L., Puget Sound Pr. & Lt. Co., Seattle, Wash.
Paschall, J. P., Portland Gen. Elec. Co., Portland, Oreg.
Ream, R. F., Gen. Elec. Co., Salt Lake City, Utah
Shearer, E. W., Bonneville Pr. Adm., Portland, Oreg.

10. Canada

Ferguson, N. H., Dominion Carbon Brush Co., Ltd., Toronto, Ont., Can.
Fraresso, M., Hydro-Elec. Pr. Comm. of Ont., Toronto, Ont., Can.
Paisley, P. P., Mica Co. of Can., Ltd., Hull, Que., Can.
Tickell, A. B., Ford Motor Co., Ltd., Windsor, Ont., Can.

Elsewhere

Carrion S., J., Com. Fed. de Elec., Patzcuaro, Mich. Mex.
de Albornoz, R. C., Serv. Com. Ind. S. A., Mexico, D. F., Mex.
Escobar B., J. V., Soc. Mex. de Credito Ind., S. A., Mexico, D. F., Mex.
Hernandez M., A., Fed. Com. of Elec., Mexico, D. F., Mex.
Lazcano R., L., Contractor & Cons. Engr., Coyoacan, D. F., Mex.
Letourneau, R., North. Elec. Co., Montreal, Que., Can.
Lira Z. E., Fed. Com. of Elec., Mexico, D. F., Mex.
Melgarejo P., A., Mex. Lt. & Pr. Co., Ltd., Mexico D. F., Mex.
Mora V., L., Cons. Engr., Mexico D. F., Mex.
O'Farril, C. C., Contractor, Mexico, D. F., Mex.
Parent, L. P., P. McCuaig, Ltd., Montreal, Que., Can.
Prakash, C., Stand. Tel. & Cables, London, England
Rowland, R. R., Johnson & Phillips Ltd., London, England
Ruiz V., J. I., Contractor, Mexico, D. F., Mex.
Strohbach, W. C., Cia Nac. de Elec., S. A., Torreon, Mex.
Sturgeon, J. R., Aircraft Ministry, Malvern, England
Toro Ochoa, H., Lt. & Pr. Co., Medellin, Col., S. A.
Van Ocken, A. H., Southern Ry., Southampton, England
Watts, K. F., Govt. Rys., Sydney, Aust.

Total to grade of Associate
United States and Canada, 141
Elsewhere, 19

Continuing Progress Reported at ECPD 12th Annual Meeting

In spite of continuing wartime difficulties, further progress was reported toward the general objectives of the Engineers Council for Professional Development, at the 12th annual meeting of council held in New York, N. Y., October 20–21, 1944. As stated by Chairman Everett S. Lee (F '30) in his report to the council for the year: "This has been another substantial year for ECPD. The personal contributions to the work in this period of difficulty have been outstanding." ECPD is a joint organization of eight national engineering societies, including AIEE, and its general objective is the enhancement of the professional status of the engineer. Its other constituent organizations are the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, American Institute of Chemical Engineers, the Engineering Institute of Canada, Society for the Promotion of Engineering Education, and the National Council of State Boards of Engineering Examiners.

Development of a program for the accrediting of technical institutes is one of the outstanding achievements reported for the year. Patterned after the program of accrediting engineering-college curriculums that has proved so successful for some ten years, the new program for accrediting technical institutes will be administered by a subcommittee of the ECPD standing committee on engineering schools. The new subcommittee will be headed by Dean H. P. Hammond of Pennsylvania State College.

Approval of a revised general reading list for junior engineers was among other important actions taken by the council at this meeting, and publication of the list for general circulation was authorized. Progress also was reported toward completion of the manual for junior engineers that has been in process for some time.

Continuing progress of the measurement and guidance program of the committee on student selection and guidance was reported. There is an increase in the number of students being tested during the current year, and a year or two more will be required for completion of the project. The joint cooperative program between the ECPD and the Society for the Promotion of Engineering Education aimed at developing professional attitudes in students was carried forward during the year, many teachers expressing the opinion that the professional aspects of engineering should be brought into all courses.

Because the engineering schools still are operating under wartime programs, very few inspections of engineering curriculums were made during the year. However, plans are being made for reappraisal of all curriculums after the war. In previous years, this work has been considered one of the outstanding achievements of ECPD.

Further details pertaining to the aforementioned activities were presented in the annual reports of the four standing committees of ECPD. Several special committees reported on their work during the year, and the various society representatives presented their annual reports to the council. Details of these reports are given separately in this article.

Supplementing the business sessions, an evening dinner meeting was held at which the general question, "What is ahead in engineering education?" was discussed. This is discussed elsewhere in this item. Related to this discussion was an official action taken during one of the business sessions endorsing the SPEE report on engineering education after the war (*EE, Aug. '44, p. 319*).

Expenditures during the year totalled $11,927.72, and a budget totaling $14,471.23 was adopted for 1944–45. ECPD income is derived chiefly from contributions from the constituent societies (AIEE $1,700 for 1944–45). For the past several years, these have been augmented by an annual contribution from Engineering Foundation, which has alloted $5,000 again for the ensuing year.

Officers and Committees for 1944–45

E. S. Lee (AIEE) engineer, General Electric Company, Schenectady, N. Y., was re-elected chairman, and James W. Parker (ASME) president, general manager, and director, The Detroit Edison Company, Detroit, Mich., vice-chairman. R. L. Sackett, assistant to the secretary, American Society of Mechanical Engineers, New York, N. Y., was named secretary, and George T. Seabury, secretary, American Society of Civil Engineers, New York, N. Y., assistant secretary.

In addition to these four officers, members of the executive committee for 1944–45 are:

George W. Burpee (ASCE), president, General Aniline and Film Corporation, New York, N. Y.; W. B. Plank (AIME), professor of mining, Lafayette College, Easton, Pa.; R. S. Goetzenberger (ASME), vice-president, Minneapolis-Honeywell Regulator Co., Philadelphia, Pa.; O. W. Eshbach (AIEE), dean, Northwestern Technological Institute, Northwestern University, Evanston, Ill.; S. D. Kirkpatrick (AIChE), editor, *Chemical and Metallurgical Engineering*, New York, N. Y.; D. B. Prentice (SPEE), president, Rose Polytechnic Institute, Terre Haute, Ind.; C. R. Young (EIC), dean, faculty of applied science and engineering, University of Toronto, Toronto, Ontario, Canada; C. C. Knipmeyer (NCBSEE), professor of electrical engineering, Rose Polytechnic Institute, Terre Haute, Ind.

The following representatives of ECPD member societies were announced for the 1944–47 term:

Reappointments—W. R. Chedsey (AIME), consulting engineer, Columbus, Ohio; J. W. Parker (ASME), president, general manager, director, The Detroit Edison Company, Detroit, Mich.; S. D. Kirkpatrick (AIChE), editor, *Chemical and Metallurgical Engineering*, New York, N. Y.

New appointments—S. C. Hollister (ASCE), dean of engineering, Cornell University, Ithaca, N. Y.; E. C. Stone (AIEE), vice-president and general manager, Duquesne Light Company, Pittsburgh, Pa.; H. S. Rogers (SPEE), president, Polytechnic Institute of Brooklyn, Brooklyn, N. Y.; C. J. Mackenzie (EIC), president, National Research Council, Ottawa, Ontario, Canada; Carl L. Svensen (NCSBEE), secretary, Texas Board of Registration for Professional Engineers, Austin, Texas.

Newly elected committee chairmen for 1944–45 are:

Committee on professional training, C. A. Pohl, consulting engineer, New York, N. Y.

Committee on professional recognition, N. W. Dougherty, dean of engineering, University of Tennessee.

Committee on information, George A. Stetson, editor, American Society of Mechanical Engineers, New York, N. Y.

The following committee chairmen were re-elected for the coming year:

Committee on student selection and guidance, A. R. Cullimore, president, Newark (N. J.) College of Engineering.

Committee on engineering schools, D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind.

ECPD Committee Reports for 1943–44

Reports of its four standing committees were presented, reviewing progress during the year in the fields of student selection and guidance, engineering schools, professional training, and professional recognition. Six special committees also reported their activities. The following abstracts of these various reports give a cross section of the significant accomplishments of ECPD during the year.

STUDENT SELECTION AND GUIDANCE

The advisement, counseling, and guidance of students about to enter college, and the development of a battery of tests to aid in the proper selection of engineering students were the two projects with which the committee on student selection and guidance concerned itself during the year, reported A. R. Cullimore, chairman. Work on counseling has been carried on as formerly through a subcommittee whose members have interested themselves in contacting prospective engineering students, and in the distribution of "Engineering as a Career" and other material published by ECPD. The scope of this work has been extended considerably during the past year. More than 2,000 copies of "Engineering as a Career" were distributed to the Army through the Science Research Associates, and many were requested and distributed by the Veterans Administration. The committee envisions a great responsibility in connection with returning veterans and their advisement plans to extend this veterans' service greatly during the coming year.

During the first fiscal year of the measurement and guidance program being conducted jointly by the committee on selection and guidance and the Carnegie Foundation for the Advancement of Teaching 4,500 students were tested, involving the administration and interpretation of 27,000 separate tests. During the first two months of the second year, tests have been given to 3,150 students representing a total of 22,000 tests. Indications are that the number of students tested in the

second year will be considerably more than double the number handled in the first year. The major part of the financial support of this project is contributed by the Carnegie Foundation, only about eight to ten per cent of the cost being carried by ECPD. The continued and expanding interest of the Carnegie Foundation has carried the project into its second year and beyond the experimental stage, reported Chairman Cullimore.

It is planned to extend the services offered by the project to other institutions of reputable and recognized character, while retaining the original group for further study and research. It is believed that the tests will be extremely valuable also in connection with student selection and guidance of veterans. In September 1943, K. W. Vaughn director of the project, was appointed special consultant to the Army Specialized Training Division. The project has co-operated officially with the ASTD, completed several researches into the effectiveness of selection tests, and assisted in the editing of test materials and the preparation of official manuals for purposes of the ASTD. Since March, 1944, the project office has formally co-operated with the Veterans Administration through certain testing and guidance centers established at the Newark College of Engineering and the College of the City of New York.

ENGINEERING SCHOOLS

Development of a program for the accrediting of technical institutes was the most important accomplishment of the committee on engineering schools reported by chairman D. B. Prentice of that committee. The basis of accrediting the technical institutes is patterned after the procedure successfully followed for some years in accrediting engineering-college curriculums. The essential substance of the subcommittee's report already has appeared in *Electrical Engineering* (*July '44, pp 277–8*).

As announced in 1942, the committee on engineering schools has held its inspections of engineering-college curriculums to a minimum during the past two years. However, a few inspections were necessary in order to complete studies already begun or to prevent injustices to certain institutions. During the past year 13 curriculums at six institutions were investigated, and special consideration was given to eight curriculums at other institutions. The committee recommended to Council that the provisional accrediting of 31 curriculums at 16 schools be temporarily extended. The present status of the accrediting program, including the recommendations of the committee for 1944, is indicated in Table I. As in previous years, the committee gave consideration to a variety of miscellaneous matters that came to its attention.

PROFESSIONAL TRAINING

The activities of the committee on professional training during the past year have been devoted to advancing two projects which are aimed to be helpful to the younger engineer in his professional training, namely: revision of the reading list for junior engineers, and preparation of a manual for junior engineers. John C. Arnell was chairman of this committee during the year.

Recognizing that the existing reading list has continued without revision since it was issued originally in 1936, the junior committee of this committee has been engaged in revising the list and is considering other books recommended for inclusion. The junior committee has constantly kept in mind that the chief purpose of the list is to serve as an introduction to vast territories of human achievement and to stimulate the interest of the junior engineer in fields that may be explored with attendant cultural profit. As reported elsewhere, the revised list presented with the report of this committee was approved by the council for publication and distribution.

Further progress was reported on the manual for junior engineers which has been under development for some time. W. E. Wickenden (F '39) president, Case School of Applied Science, Cleveland, Ohio, who had accepted the authorship of the manual, reported to the committee that the material for the manual is about two thirds completed and the actual manuscript about one fourth complete. The committee plans to have the manual available for distribution in the postwar period when there will be a renewal of the more normal occupations of teacher, student, junior engineer, and employer.

PROFESSIONAL RECOGNITION

The ECPD committee on professional recognition directed its immediate efforts during the past year to the young engineer, particularly in the engineering schools, reported Chairman Charles F. Scott (HM '29). These efforts have been carried on jointly with a committee of the Society for the Promotion of Engineering Education in a plan for co-operative action announced last year. These co-operative activities have been directed to the engineering teacher "to arouse his responsibility in promoting the nontechnical phases of the make-up of the engineer and the qualities that contribute to the building of the profession."

Revision of the booklet, "The Second Mile," was carried out during the year under sponsorship of this committee, under the title "The Second Mile—a Resurvey." Some 20,000 copies of the original booklet were distributed. Doctor Scott reported that 30 per cent more copies of the new booklet had been sold in the first few months after it was issued than were sold in two years of the earlier edition.

REPORTS OF SPECIAL COMMITTEES

Progress regarding the proposed canons of ethics for engineers has been very slow during the past year, reported Chairman D. C. Jackson (F '12) of the committee on principles of engineering ethics. Three societies have approved the canons in their present form, some have offered suggestions for revisions, and others so far have reported no action. An article outlining the history and present status of the canons by Chairman Jackson appears elsewhere in this issue.

No committee action was reported by the committee on employment conditions for engineers, but Chairman Van Tuyl Boughton presented a report summarizing actions taken in this field by ECPD constituent societies. He reported that up to July 15, 1944, 30 of the 64 local sections of the American Society of Civil Engineers had amended their constitutions to provide for establishing collective-bargaining units, five had rejected the proposal by vote of section members, and in five the board of direction had decided not to submit the proposed plan to vote. Actual organization of the collective-bargaining committees was reported as slow.

The committee on information carried on its usual activities during the year, according to Chairman Stephen L. Tyler. Two general releases were issued, one pertaining to new officers and committee chairmen, and the other to the accrediting of technical-institute curriculums. The committee also issued the annual report for 1942–43 and co-operated with other committees in issuing other material.

A special committee presented a proposed procedure for the guidance of the council in accepting contributions that might be offered by individuals or groups other than the participating bodies in support of the work of ECPD; this procedure was adopted. It

Table I. Status of All Curriculums Submitted for Accrediting as of October 20, 1944

Curricula	Total Curriculums Submitted	Fully Accredited	Provisionally Accredited	Not Accredited	Action Pending or Deferred	Accredited Curriculums Discontinued	Options Under Accredited Curriculums
Aeronautical engineering	17	14	0	2	1	0	26
Agricultural engineering	11	3	0	8	0	0	1
Architectural engineering	18	13	1	3	0	1	2
Ceramic engineering	10	9	1	0	0	0	0
Chemical engineering	88	42	11	35	0	0	0
Civil engineering*	137	107	20	10	0	0	0
Electrical and communication engineering	133	108	18	7	0	0	0
Electrochemical engineering	2	0	0	0	0	2	0
Fuel technology	1	0	1	0	0	0	1
General engineering	14	8	0	5	1	0	0
Industrial engineering	24	21	0	3	0	0	10
Mechanical engineering	123	102	15	6	0	0	0
Metallurgical engineering	31	27	2	2	0	0	7
Mining (and geological) engineering	42	26	6	9	0	1	0
Naval architecture and marine engineering	3	3	0	0	0	0	1
Petroleum engineering**	17	11	3	2	0	1	6
Sanitary (and public-health) engineering	5	5	0	0	0	0	23
Others (specialized)	7	0	0	7	0	0	0
Total	683	499	78	99	2	5	

* Includes building engineering and construction, engineering mechanics, and transport engineering.

** Includes petroleum and natural-gas engineering and petroleum refining.

authorizes the ECPD chairman, subject to the approval of the executive committee in each instance, to solicit from sources other than the participating bodies such funds as may be necessary or desirable for the furtherance of the general objectives or specific projects of ECPD.

Upon recommendation of a special committee on rules of procedure–representation, C. C. Willams, chairman, an amendment to the rules of procedure was adopted limiting membership in standing committees to two terms (of three years each) unless the council in session by vote of two thirds of the representatives present authorizes additional terms. The rules formerly stipulated that members should not serve more than two *consecutive* terms.

To consider specific requests for representation on the council by the Institute of Ceramic Engineers and the Institute of Radio Engineers, a special committee on representation had been appointed. The following recommendations, resulting from a study of the general society relationships in ECPD by that committee, were presented by Chairman W. B. Plank:

1. Until the Joint Conference of the Engineering Societies determines the conditions of association in professional matters with other engineering societies, the ECPD would be unwise to make any move in that direction.

2. Before any additional engineering society be invited into participation in the ECPD, if the field of that society be related to the field of any existing participating society, then the matter be referred to that participating society for its approval.

3. The committee on engineering schools should see that on each delegatory committee there is at least one qualified representative of each curriculum to be considered for accrediting.

4. All national engineering societies in the United States and Canada should be placed on the mailing list to receive ECPD reports and other literature.

Postwar Education Discussed at Dinner

For the second consecutive year, the discussions at the ECPD annual dinner centered around engineering education after the war. The principal address on this subject was delivered by George Granger Brown, chairman, department of chemical and metallurgical engineering, University of Michigan, and one of the ECPD representatives from the American Institute of Chemical Engineers. Three other viewpoints on the subject were presented in scheduled discussions by Dean C. R. Young of the University of Toronto; George W. Burpee, consulting engineer, Coverdale and Colpitts, New York, N. Y.; and Colonel C. E. Davies, secretary, American Society of Mechanical Engineers. ECPD Chairman Everett S. Lee acted as toastmaster.

Declaring that what is ahead is whatever we make it, Doctor Brown outlined five goals for engineering education in the future:

1. An understanding of the scientific principles and facts upon which the profession is based.

2. A facility in thinking and expression.

3. An appreciation of and skill in the experimental method as a means of finding the truth.

4. Sound principles of honesty, fairness, and the democratic spirit, with appreciation of the human and economic problems of modern society.

5. The beginnings of a philosophy and integration of skills in using the materials and energy of nature for the benefit of mankind.

Emphasizing that teachers of engineering are faced with an unprecedented opportunity and responsibility in the postwar period, Doctor Brown predicted that veterans are more likely to seek engineering education after the war than liberal education. For this reason, accelerated programs probably will have to be continued in engineering schools for at least two years after victory.

In presenting the Canadian viewpoint, Dean Young agreed that the majority of students after the war will want training in the sciences. Characterizing physicists as being concerned with the laws of nature and engineers with the needs of man, he urged that the colleges first provide education in the vocations and later liberal education at an adult level.

In presenting industry's viewpoint, Mr. Burpee said that industry will provide the specialized courses required by returning veterans. These will be a combination of work and training to broaden cultural interests and prepare the engineer for leadership. Mr. Burpee urged that the colleges teach a student how to make best use of his talents and give him an inspiration to go out after graduation and "make his place in the sun."

Educational functions and professional responsibilities are closely linked, stated Colonel Davies in discussing the societies' viewpoint. He declared that the societies have a responsibility in encouraging youngsters who have the capacity to grow to develop their qualities of leadership. He declared that ECPD is facing a greater opportunity than ever before to further its objectives in the postwar period.

Report of AIEE Representatives

"ECPD was able to render a novel service to the Institute during the past year," stated O. W. Eshbach, who presented the annual report of AIEE representatives to ECPD at the meeting of the council. "The board of examiners asked us to determine whether Melbourne University (Australia) came within the usual interpretation in this country of a school of recognized standing. The inquiry arose as a result of a request for enrollment of a number of students from Melbourne University, and the Institute's bylaws specify that students may be enrolled only from schools of recognized standing. We transmitted the inquiry to the chairman of ECPD's committee on engineering schools who was good enough to investigate and who was able to inform us that the engineering-degree students in good standing at the University of Melbourne would qualify for student membership in the Institute. We were happy to be able to transmit this finding to the board of examiners.

"During the year the Sections committee and the committee on Student Branches brought to the attention of the chairmen of the Sections and Branches both the ECPD pamphlet, "Will You Help?" and Chairman Lee's paper, "The Engineer in the Postwar World" (*EE, June '44, pp 218–20*). These were transmitted by special letter urging the distribution and the use of this material cooperatively with the sections and branches of the other engineering societies in carrying out locally the general objectives of ECPD.

"On page 301 of the August 1944 issue of *Electrical Engineering* will be found a report of Mr. Lee's appearance at the delegates' conference during the summer technical meeting in St. Louis, Mo., where he expanded on the theme, "Will You Help?"

During the year, and upon the recommendation of the chairman of the committee on Student Branches, the Institute purchased 5,000 copies of the revised pamphlet by W. E. Wickenden "The Second Mile—A Resurvey."

WAR PROGRAM • •

Aircraft Gunsight Extends Firing Range and Accuracy

An electronic gunsight of greater accuracy and less complexity of operation than the mechanical type previously used recently was installed on United States bombers. The gunsight was described by Erwin Hale, engineer of the Fairchild Camera and Instrument Corporation, and coinventer with Irving Doyle of the improved gunsight, at the national fall meeting of the Institute of the Aeronautical Sciences in Dayton, Ohio.

The new gunsight, designated as the *K*-8 by the Army, has demonstrated an accuracy against the enemy's fastest airplanes approaching that of stationary guns firing at stationary targets. The device has extended the range of the 50-caliber machine gun to more than 1,000 yards in contrast to its former range of 400 to 600 yards. Even over this increased range a much higher accuracy of gunfire is obtained. The power of the *K*-8 as an almost impregnable defense against pursuit airplanes is manifest when it is realized that the latter must approach within 200 to 400 yards of a bomber to score effective hits.

Installed in aircraft gun turrets, the *K*-8 gunsight automatically corrects deflection between the line of sight and the gun. It is only necessary that the gunner keep a ring of light in the sight aligned with the target, for the gun to be corrected for all factors affecting the course of the bullet as well as for the target's relative velocity. All this computation is done electrically, and the final voltage representing the desired offset is relayed to an electronic servosystem which corrects for it.

The *K*-8 is an advance over the earlier mechanical gunsights in that it is instantaneous whereas computations attendant upon use of the mechanical ones entailed a time lag. It is complete, correcting as it does for all factors affecting the projectile. Mechanical computers have had to neglect the effect of altitude and indicated air speed, and only two of the remaining factors could be used at one time for a given computation. Finally, the *K*-8 is compact. If any mechanical gunsight were to incorporate corrections for all the factors accounted for by the *K*-8, its mechanism would fill the gun turret.

The *K*-8 has the added recommendation of ease of manufacture. Many of the parts of the electric computer are standard radio equipment, and a large part of the assembly work consists of wiring up the various components. Considerably less skilled labor is employed in its production, resulting in lower first cost and cheaper maintenance.

Operation of the *K*-8 is relatively simple for a gunner. Once he is experienced in operation the power gun turret itself, five hours instruction should develop the competence necessary to operate the *K*-8. The

computing system developed for the gunsight is expected to see more widespread use in peacetime. It is particularly adaptable for problems involving several variables, which are difficult or impossible to handle by mechanical means.

Explosives Sink Telegraph Poles for Signal Corps

An ingenious method has been developed by the United States Signal Corps for sinking telegraph poles in very loose sand where the ordinary method of boring with an earth auger fails, because sand fills the hole the moment the borer is withdrawn.

A camouflet tube, which is a land mine so charged that it burrows into the ground rather than merely blasting a surface crater, is driven to a depth of five feet. In the tube is inserted a primer and two pounds of explosive. Once this explosive is planted underground, the tube is removed, and the loose sand covers the explosive charge and fills in the excavation made by the mine. The telegraph pole is held upright on the ground directly over the charge by four light guy ropes. When the buried charge is fired the pole sinks to a depth of five or six feet and is embedded firmly by the loose sand falling in around it as packing. If the amount of explosive and the number of primers are increased, poles can be sunk to greater depths.

Silicone-Rubber Gaskets Used in Navy Searchlights

Silicone rubber, a new form of silicone developed in the General Electric Research Laboratory, which retains its elastic properties at temperatures as low as 60 degrees below zero or as high as 575 degrees Fahrenheit, is now being used for gaskets for Navy searchlights which are employed to direct the fire of guns at night.



A gasket of silicone rubber, which remains elastic even at the high temperatures encountered near a high-intensity electric-arc light, is applied to the lens of a 24-inch Navy searchlight

The searchlights must be as close as possible to the battery, and this subjects them to severe jarring each time a gun is fired. To prevent breakage, the front glass has to be mounted with a shock absorber, and the high temperature of the electric arc inside requires that it resist heat.

Formerly asbestos was used, since it was heat-resistant, although it lacks resiliency, a necessary property of the shock absorber to prevent breakage of the glass during gun blast. Silicone rubber now combines the desired resiliency with heat resistance.

Silicone-rubber gaskets also are used on the 12-inch signaling searchlights for the Navy and on the turbosuperchargers in *B*-29 bombers where they have been found to meet the necessary specifications. Tests show that a silicone-rubber gasket which seals the cover plate of the compressor casing of a supercharger retains its resiliency after 150 hours of continuous operation at 300 degrees Fahrenheit.

Silicone rubber is a polymer the backbone of which consists of a series of units, each made up of a silicon and an oxygen atom linked together. Attached to the atoms of silicon, on the side, are groups of hydrocarbons. At the present time all the silicone rubber being produced in the United States is being used in high-priority war jobs.

POSTWAR

Stimulus to Industry and Employment in REA Program

A potential $5,546,283,000 outlet for goods and services could be realized by a five-year rural electrification program for 3,655,000 rural homes, according to detailed estimates accompanying a proposal recently submitted to the United States Department of Agriculture interbureau committee on postwar programs by the Rural Electrification Administration.

The report is based on data from the 1940 census, from reports of REA-financed cooperatives throughout the country, from actual surveys of unelectrified farms in 600 counties, and on the findings and reports of the various state postwar planning committees. Estimates of line construction costs were made on a state basis, using the prewar cost of line per consumer as a factor. Expenditures for wiring, appliances, and plumbing installations were estimated on the basis of prewar experience modified by technological improvements during the war. The latter computations were made on a national basis.

More than 6,344,000 occupied rural dwellings now do not have central-station electric service, according to the REA surveys. Some of these are close to large cities and probably can be served by extensions from urban power systems; others located in areas of sparse population or isolated by physical barriers probably cannot be reached within the five-year period. But over 3,000,000 can be electrified within the limits of the program, the report forecasts, by extensions from existing rural power systems or from new ones built by local interests wherever they are needed.

The report contemplates construction by both public and private financing though it is the conclusion of the REA committee that co-operative enterprise will provide the principal means of carrying out the proposed plan.

The market which will be opened as a direct result of the construction program has been estimated in detail, and the report refers to but does not estimate the volume of such general building renovation and purchases of new nonelectrical home furnishings as have followed rural electrification in the past.

Cost of constructing the new lines for the complete program is put at $1,020,000,000. On a basis of one man-year of labor for each $2,000 spent, this undertaking should supply 510,000 man-years of labor.

The average newly connected farm will spend $145 for wiring and $400 for appliances and equipment, the preliminary report indicates, and in addition 35 per cent of them will install pressure water systems and plumbing costing $225. Other rural establishments—nonfarm residences and the like, making up 25 per cent of the total—will spend $90 each for wiring and $200 each for appliances, and 25 per cent of them will install plumbing averaging $225.

These expenditures should reach a total of $2,026,241,000 and require 1,013,120 man-years of labor. Wiring accounts for $479,723,000 and 239,861 man-years of this estimate, appliances and equipment for $1,279,240,000 and 639,620 man-years, and plumbing for $267,278,000 and 133,639 man-years.

The report also includes an estimate of the amount of new business arising from additional electrical installations by rural consumers already receiving service. It is anticipated that this will be even greater than that from similar installations by newly served consumers, aggregating $2,500,042,000 and providing 1,250,000 man-years of labor. These are divided as follows: The 2,557,000 presently electrified farms will spend an average of $40 for additional wiring and $300 for new electric equipment, and half of them will average $225 each for plumbing. The 6,800,000 nonfarm rural establishments now receiving service will average $25 each for additional wiring and $150 for electric appliances, and ten per cent of them will install plumbing at an average cost of $150.

The estimated totals for these additional expenditures by already electrified rural places are: Wiring, $272,280,000, providing 136,000 man-years of work; appliances and equipment, $1,787,100,000 and 893,000 man-years; and plumbing, $440,662,000 and 220,000 man-years.

The report does not attempt to break down the five-year figures between private financing and public funds. It does, however, cite a possible three-year program of REA loans for construction of distribution lines, rehabilitation of existing facilities, establishment of additional generation and transmission facilities, and financing installations of plumbing and appliances and other equipment using electricity. Loan funds of $579,000,000 would be needed to effect such a program.

This sum represents new investment.

REA officials pointed out that about $113,000,000 of REA loans that have already been approved have not yet been advanced to borrowers, largely because of wartime restrictions on construction; but approved loans can be used for construction almost immediately upon the lifting of restrictions. REA has applications on hand for additional loans totaling almost $110,000,000; any approved applications from this group would, of course, constitute part of the three-year program, if it is authorized.

The report stressed the principle of area coverage as all-important to both its programs. Otherwise, the report states, there will remain many unserved and unservable pockets, because the cost of bringing electricity to these sparsely settled communities separately would be prohibitive.

Bombardiers Practice in Ground Units



In this prefabricated "7*A*-3 Horizontal Bombings and Dead Reckoning Trainer," which can be assembled indoors or out, are a replica (center) of the bombardiers compartment of the Navy's *PB-4Y* and the Army's *B-24* heavy bombers; an instructor's compartment, shown at the left; and a floor screen upon which is projected the image of a target site. Training in bombing, dead-reckoning navigation, intercommunication procedure, and evasive action all are given in the new trainer manufactured by the International Business Machines Corporation. When setting up a bombing problem, the instructor reproduces the conditions of actual flight. By means of his instruments he can control altitude, air speed, and direction. Reproduction of even the rarefied atmospheres of high altitudes (to accustom the bombardier to working in heavy clothing) and the noise and vibration of the motors complete the illusion. The instructor can follow the moves made by the student, and conditions can be held stationary at any point for correction of errors or discussion of method. A ring of light on the image registers the accuracy of each bomb release. The trainer not only diminishes the extra expense and time and greater danger of target practice from an airplane but also obviates the period of readjustment formerly existing between ground training and practice flights.

INDUSTRY

Status of Television Discussed at FCC Hearing

The television receiver of the future will cover, in a single receiver, carrier frequencies from 540 kilocycles to 300,000 kilocycles and will receive amplitude- and frequency-modulated broadcasts and television programs, according to C. B. Jolliffe (M '34), chief engineer of the RCA Victor division of the Radio Corporation of America, in a statement made before the Federal Communications Commission at a hearing on frequency allocations. If television service is to be located in the frequency bands below 300 megacycles and also above 400 megacycles, it may be possible to build into a single cabinet a receiver or receivers which will accept all services, either black and white or color, and regardless of the standards which may be used in the different frequency bands.

During the war, RCA alone has had more than 1,000 engineers working on the research, development, design, and production of radio equipment for the Armed Forces. They have worked in the field of vacuum tubes, both transmitter and receiver, cathode-ray tubes, low-, high-, very high-, and super high-frequency transmitting and receiving equipment, modulated, continuous wave, and pulsed. This experience will be reflected in peacetime production.

Before the war, television transmitters were available that were capable of operating at frequencies between 40 and 108 megacycles with a power up to a five-kilowatt peak. Although the war has interfered with the production of the necessary higher-power transmitters and tubes, television transmitters that will generate power of 50 kw up to 108 megacycles and five kilowatts up to 300 megacycles are in the process of design. Prewar receivers, capable of producing a picture approximately 15 by 20 inches, will be replaced by a receiver, now in the experimental stage, that will reproduce a picture approximately 18 by 24 inches in size on a translucent screen with adequate brightness to be viewed in a fairly well-lighted room.

In regard to proposals to move television to frequencies above approximately 450 megacycles, Mr. Jolliffe contends that such a step would necessitate the development of a new and untried system involving a great deal of experiment and change in the present equipment before it can be presented to the public. It is estimated that this conversion would take at least five years during which time television could be reaching the American people on the lower frequencies. According to Mr. Jolliffe, the primary purpose of moving to the higher frequencies is to obtain adequate color television, and this feature should be offered as an addition to the present system rather than as a replacement.

In order to give television its fullest scope for expansion, Mr. Jolliffe recommends that the FCC reaffirm its authorization for commercial television on frequencies below 300 megacycles and that it authorize the use of frequencies above approximately 450 megacycles for the experimental development of a new television system.

Anniversary Movie Presented. With the presentation of a full-length motion picture entitled *Heritage for Victory*, the Western Electric Company commemorated its 75th anniversary in November 1944. The picture, which was produced in Hollywood, dramatizes the growth of the organization over a period of three quarters of a century and thus also depicts the history of the telephone and the telegraph. It will be shown to the Western Electric Company's nearly 100,000 employees throughout the country.

CBS Soon to Broadcast Single-Transmitter Television

The first commercial transmitter which will broadcast both sight and sound signals on the same carrier frequency is being manufactured by the Federal Telephone and Radio Corporation for the Columbia Broadcasting Company. The new transmitter will be installed atop the Chrysler Building in New York, N. Y., and will broadcast programs produced in the studios of WCBW in the Grand Central Terminal Building in New York.

The carrier frequency for the composite signal will lie between 450 and 500 megacycles and the video frequency band will be ten megacycles. Combining visual and aural transmission on the same carrier frequency means a lower first cost, less power consumption, less space requirement, and fewer high-power tubes for the broadcaster and a simplified receiver requiring less tubes for the listener. The fine-screen broad-band high-frequency transmission, which will be introduced, marks an improvement over the coarse-screen picture currently broadcast.

A degree of sharpness and detail twice that attained in present pictures is said to be possible. Larger home screens, greater focal depth, and sharper clarity of objects in motion will accompany the new type of transmission. Extension of the television service beyond the station's present range of 11,000,000 people will depend upon public acceptance of the programs and availability of intercity transmission facilities.

HONORS

Highest Engineering Award Goes to John L. Savage

John L. Savage, internationally prominent engineer long associated with the United States Bureau of Reclamation, has been announced as the recipient for 1944 of the John Fritz Medal, oldest and generally conceded to be the highest award of the engineering profession in America. The citation recognizes his "superlative public service in conceiving and administering the engineering of mammoth dams, both in America and beyond the seven seas."

Born in Wisconsin in 1879, Mr. Savage was graduated from the University of Wisconsin with the degree of bachelor of science in civil engineering in 1902. He entered the United States Reclamation Service in 1903 and, except for a period from 1908 to 1916 as consulting engineer with A. J. Wiley, Boise, Idaho, has continued that connection throughout his entire career. Since 1924 he has been chief designing engineer for the Bureau and as such has had charge of the design of all the great dams under its jurisdiction including the Boulder, Grand Coulee, and Shasta Dams. Furthermore he has been adviser on the design of the Tennessee Valley Authority dams and carried out assignments in Puerto Rico, South America, India, and Australia. At present he is engaged in a consulting capacity on important works in India. He holds the honorary degree of doctor of science from the University of Wisconsin and the gold-medal award for distinguished service of the Colorado Engineering Society. He has been invited as honorary lecturer before the Institution of Civil Engineers of Great Britain. In 1941 he was elected to honorary membership in the American Society of Civil Engineers.

The John Fritz Medal is awarded annually by a board composed of four representatives of the American Institute of Electrical Engineers, the American Society of Mechanical Engineers, the American Institute of Mining and Metallurgical Engineers, and the American Society of Civil Engineers, for notable scientific or industrial achievements, regardless of nationality or sex.

Lawrence D. Bell Receives Aeronautics Medal. The Daniel Guggenheim medal for achievement in aeronautics has been awarded to Lawrence D. Bell for "achievement in design and construction of military aircraft and for outstanding contributions to the methods of production." Since forming the Bell Aircraft Corporation, Mr. Bell has specialized in the construction of fighter aircraft among which are the Airacuda, the Airacobra, and Kingcobra models. His company also is a producer of the Boeing *B*-29 Superfortress. Mr. Bell has been general manager of The Glenn L. Martin Company and the Consolidated Aircraft Corporation. Later he organized his own company. Mr. Bell has served as president of the East Coast and National Aircraft War Production Councils. He is a member of the Institute of Aeronautical Sciences and a past president of the Aero Club of Buffalo, N. Y. The award is made by a committee appointed by the American Society of Mechanical Engineers, the Society of Automotive Engineers, and the Institute of Aeronautical Sciences together with past medalists residing in the United States.

OTHER SOCIETIES

Raise AIME Member Dues

An amendment to the bylaws of the American Institute of Mining and Metallurgy providing for an increase in the annual dues of members of that organization from the former $15 to $20 was adopted recently by the AIME board of directors. Concomitant to this increase are the reduction of the dues paid by junior members to $10 and the abandonment of the $20 initiation fee formerly required of junior members wishing to transfer to the status of member, if the junior member has paid dues for ten years, either as student member or junior member, prior to his transfer.

This method of balancing the AIME budget and at the same time lowering the financial membership requirement for young engineers was adopted in preference to curtailment of any present AIME activities or publications or solicitation of outside subsidy. Among the immediate results of the new program expected by AIME president, Chester A. Fulton, are an increased allocation of funds to the papers and publications committee, an addition to the number of publications an individual member may receive annually without extra charge, and an increase in the allocation of funds available to the committee on local section appropriations. None of the additional revenue will be expended for administrative costs or for maintenance or operation of AIME headquarters in New York, N. Y.

ASRE Announces Grants for Refrigeration Research

Four grants of $500 each, sponsored by the American Society of Refrigerating Engineers, have been made to institutions engaged in refrigeration education and research, according to a recent announcement of the society.

The grants, which are made on the basis of recommendations of the ASRE committee on education and research, have been awarded to the Massachusetts Institute of Technology for research on a standard calorimetric procedure for testing of refrigeration compressors and on the pressure drop in Freon evaporators, and to the Oregon State College for projects to determine the values of film coefficient of heat transfer from metal surfaces to boiling ammonia in a flooded evaporator and to develop methods of preserving fish.

Similar grants have also been made to the University of Illinois and to the University of Tennessee.

AWS Receives Award. Presentation of the Ordnance Distinguished Service award, which was authorized on July 20, 1944, was made to the American Welding Society at the opening session of its annual meeting in Cleveland, Ohio. The presentation was made by Colonel S. B. Ritchie, assistant chief of research and development, Office of the Chief of Ordnance, in recognition "of outstanding and meritorious engineering advisory services, in war and peace, for the development and maintenance of ordnance matériel." The award was accepted by David Arnott, president of the society, and vice-president and chief surveyor of the Bureau of Shipping.

ASCE Assigns Field Secretary to Western Area

Henry M. Thackwell, a member of the American Society of Civil Engineers, has been appointed to the society's staff as field secretary, as of August 17, 1944, to represent the society in an area covering the 11 Western States and Texas. His duties formerly were handled by visits of staff members, whose headquarters were in New York City.

Mr. Thackwell's new assignment will enable him to assist the local sections and student chapters and to give guidance to groups of professional engineering employees in need of information on matters pertaining to employment conditions. He is to serve the members in any way in which he can as a staff member: to locate publishable material of professional interest, to contribute toward the activities of the administrative and professional committees, and, in general, to supervise the society's interests in his area.

AIME Elects 1945 Officers

Election of Harvey S. Mudd, president and managing director, the Cyprus Mines Corporation, Los Angeles, Calif., as president of the American Institute of Mining and Metallurgical Engineers was announced recently.

Other officers elected were: vice-presidents, Donald H. McLaughlin, vice-president and general manager, Cerro de Pasco Copper Corporation, New York, N. Y., and Leo F. Reinartz, manager, Middletown (Ohio) division, American Rolling Mill Company; and six directors, C. H. Benedict, metallurgical manager, Calumet and Hecla Consolidated Copper Company, Lake Linden, Mich.; Robert H. Morris, general manager, the Gauley Mountain Coal Company, Ansted, W. Va.; J. C. Nicholls, assistant to the president, the International Nickel Company of Canada, Ltd., Toronto,

Future Meetings of Other Societies

American Institute of Mining and Metallurgical Engineers. Annual meeting, February 18–22, 1945, New York, N. Y.

American Society of Civil Engineers. Annual meeting, January 17–19, 1945, New York, N. Y.

Institute of Radio Engineers. Winter technical meeting, January 24–27, 1945, New York, N. Y.

Institute of the Aeronautical Sciences. 13th annual meeting, April 1945, Detroit, Mich.

Ontario; Clyde E. Weed, vice-president in charge of mining operations, Anaconda Copper Mining Company, New York; Eugene A. White, manager, Tacoma (Wash.) smelter, American Smelting and Refining Company; and William E. Wrather, director, United States Geological Survey, Washington, D. C.

AWS Announces 1945 Officers

A. C. Weigel, vice-president, Combustion Engineering Company, Inc., New York, N. Y., has been elected president of the American Welding Society, according to a recent announcement.

Other elections include: Isaac Harter, vice-president, the Babcock and Wilcox Company, Barberton, Ohio, to the office of first vice-president; W. F. Hess (M '41), assistant professor of metallurgical engineering, Rensselaer Polytechnic Institute, Troy, N. Y., second vice-president; O. B. J. Fraser, director of technical service on mill products, International Nickel Company, New York, treasurer; and M. M. Kelly, Society Headquarters, secretary. Newly elected directors are: Roger W. Clark, welding engineer, General Electric Company, Schenectady, N. Y.; L. W. Delhi, vice-president, Western Pipe and Steel Company, San Francisco, Calif.; J. F. Lincoln (F '39), president, Lincoln Electric Company, Cleveland, Ohio; and H. Malcolm Priest, engineer of the Railroad Research Bureau, United States Steel Corporation Subsidiaries.

IRE Elects Officers for 1945

Election of William L. Everitt (F '36), chief of the operational research branch, Office of the Chief Signal Officer of the United States Army, Washington, D. C., as president of the Institute of Radio Engineers, has been announced by the board of directors of that society.

Also elected were Hendrik J. Van der Bijl (M '17), chairman of the Electricity Supply Commission, Johannesburg, Transvaal, Union of South Africa, as vice-president of the institute; and for three-year terms as directors; Stuart L. Bailey, consulting radio engineer, Jansky and Bailey, Washington, D. C.; Keith Henney, editor of *Electronics*, New York, N. Y.; and Benjamin E. Shackelford (M '42) engineer-in-charge, frequency bureau, Radio Corporation of America, New York.

Engineering Foundation Selects Officers for 1945

The Engineering Foundation has re-elected A. L. Queneau, metallurgist, the United States Steel Corporation, as chairman for 1945; Edwin H. Colpitts (F '12), formerly director, the Bell Telephone Laboratories, as director; and John H. R. Arms as secretary. The new vice-chairman is A. B. Kinzel, chief metallurgist, Union Carbide and Carbon Research Laboratories, Inc.

The research procedure committee will be composed of L. W. Chubb (F '21), Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.; E. A. Prentis, Jr., Spencer, White, and Prentis, Inc.; H. E. Wessman, New York University; W. M. Peirce, the New Jersey Zinc Company; W. Trinks, Carnegie Institute of Technology; and W. A. Lewis (M '39), Armour Research Foundation. Mr. Queneau will represent the Engineering Foundation on the executive board of the National Research Council.

UET 1945 Officers Elected

The re-election of F. Malcolm Farmer (F '13), vice-president and consulting engineer, the Electrical Testing Laboratories, New York, N. Y., as president of United Engineering Trustees, Inc., has been announced by that society.

Other officers elected include vice-presidents Arthur S. Tuttle, consulting engineer, and Everett S. Lee (F '30), engineer in charge, general engineering laboratory, General Electric Company; George L. Knight (F '17), chairman of the finance committee; A. L. Queneau, chairman of the real estate committee; Albert Roberts, treasurer; C. R. Jones (M '30), assistant treasurer. John H. R. Arms, general manager of the corporation, continues as secretary.

Applications Available for Jewett Fellowships

Applications for the Frank B. Jewett Fellowships for post-doctorate research in the fundamental physical sciences may now be obtained from the Frank B. Jewett fellowship committee, Bell Telephone Laboratories, 463 West Street, New York 14, N. Y.

The applications should be returned to the committee before January 1, 1945, and announcements of awards will be made on March 1, 1945. Applicants who, because of location, will be unable to obtain and return an application before the closing date may apply by a letter which should contain a brief biographical statement, information as to education and previous professional work, a report of research completed or in process, a discussion of the research which the applicant proposes to pursue if appointed, and the name of the institution where he wishes to work. Three or four names should be furnished for references regarding the applicant's scientific qualifications and potentialities.

Westinghouse Company Forms New Educational Department

Formation of a new educational department, co-ordinating all of the company's relations with schools, colleges, and universities, has been announced by the Westinghouse Electric and Manufacturing Company, Pittsburgh, Pa.

The new department will consist of three main divisions: school service, which provides teaching aids and science information to schools and other educational institutions, and is directed by Charles W. McLean; university relations, which administers the company's scholarships and fellowships and supervises the recruiting of graduate student employees under the management of Howard C. Madsen (A '42); and the student training division, with O. D. Montgomery as manager, which supervises the training of all graduate students for the company.

LETTERS TO THE EDITOR

INSTITUTE members and subscribers are invited to contribute to these columns expressions of opinion dealing with published articles, technical papers, or other subjects of general professional interest. While endeavoring to publish as many letters as possible, Electrical Engineering reserves the right to publish them in whole or in part or to reject them entirely. Statements in letters are expressly understood to be made by the writers. Publication here in no wise constitutes endorsement or recognition by the AIEE. All letters submitted for publication should be typewritten, double-spaced, not carbon copies. Any illustrations should be submitted in duplicate, one copy an inked drawing without lettering, the other lettered. Captions should be supplied for all illustrations.

Postwar Curriculums in Electrical Engineering

To the Editor:

The article "Postwar Curriculums in Electrical Engineering" by R. G. Kloeffler in the October issue of *Electrical Engineering* interested me very much as I had some thoughts along the same line.

I think Mr. Kloeffler has brought to the fore a most important factor, which properly can influence the future of our country, not just in electrical engineering but in all fields of engineering. Mr. Kloeffler advocates the introduction of nontechnical courses into the engineering curriculum, so that the engineer will be better adapted to do his bit toward bettering the world economically,

Table I

Freshman			
English I	3	English II	3
Mathematics I	3	Mathematics II	3
Physics I	3	Physics II	3
Physics Laboratory I	1	Physics Laboratory II	1
Chemistry I	3	Chemistry II	3
Chemistry Laboratory I	1	Chemistry Laboratory II	1
Engineering drawing I	2	Engineering drawing II	2
Military science and tactics—Basic I	2	Military science and tactics—Basic II	2
Basic Electrical engineering I	1	Basic Electrical engineering II	1
Total	19	Total	19
One month summer session....Surveying			4 hours
Sophomore			
American History	3	Public speaking	3
Mathematics III	3	Technical reports	1
Physics III	3	Principles of electrons	2
Physics Laboratory III	1	Mathematics IV	3
Electrical engineering I	3	Physics IV	3
Metallurgy	2	Physics Laboratory IV	1
Metallurgy Laboratory	1	Electrical engineering II	3
Geology	1	Electrical engineering Laboratory II	1
Military science and tactics—Basic III	2	Military science and tactics—Basic IV	2
Total	19	Total	19
One month summer session....Welding, foundry, machine tools, precision tools, and shop practice			4 hours
Junior			
Economics	3	Accounting	3
20th century history	3	Electronics	4
Thermodynamics I	3	Electronics Laboratory	1
Thermodynamics Laboratory I	1	Electrical engineering IV	3
Electrical engineering III	3	Electrical engineering IV Laboratory	1
Electrical engineering III Laboratory	1	Thermodynamics II	3
Precise Electrical Instruments Measurement Laboratory	2	Thermodynamics II Laboratory	1
Advanced military science and tactics or elective	3	Advanced military science and tactics or elective	3
Total	19	Total	19
Six weeks military summer camp or summer industrial employment with written paper			4 hours
Senior			
Political science	3	Music and literature	2
Hydraulics	2	Strength of materials	2
Hydraulics Laboratory	1	Strength of materials Laboratory	1
Electrical engineering V	3	Electrical engineering VII	3
Electrical engineering VI	3	Electrical engineering VIII	3
Electrical engineering V Laboratory	1	Electrical engineering VII Laboratory	1
Industrial planning	2	Industrial management	2
Engineering applications to biological science	1	Industrial economics or Industrial applications	1
Advanced military science and tactics or elective	3	Advanced military science and tactics or elective	3
Total	19	Total	19

politically, and socially as well as in the field of engineering. I am in full accord with his ideas but also wish to expand them with some of my own.

A person who has gone to an engineering institution usually is educated principally in the field of physical science with some broadening of thoughts in the field of social science. This person may be invaluable in his own field but can offer nothing to society as a whole. How much better equipped this person would be to add something to the world, as well as himself, if he had been educated in humanistics, biological science, the use of his hands, and the general over-all picture of how these fields effect each other.

Along this line let us therefore make provisions for some more nontechnical courses in addition to those suggested by Mr. Kloeffler, such as those listed below:

Industrial planning	3 hours
Industrial management	2 hours
Accounting	3 hours
Industrial economics, or Industrial application of electrical equipment	1 hour
Engineering applications to biological science	1 hour
Geology	1 hour
Metallurgy	3 hours
Military science and tactics—basic (compulsory)	8 hours
Advanced military science and tactics (optional)	12 hours
One month summer session of practical instruction in welding, foundry, use of machining tools, and precision mechanical work	4 hours

This would give us the broad tentative schedule shown in Table I.

The foregoing would be a total of 163 credit hours and this would be quite a heavy schedule for a student. At the same time there are not quite enough electrical-engineering courses (40 credit hours) to give the student all the specific electrical knowledge he might desire, but, on the other hand, the graduate would have a well-rounded education that would fit him better for the over-all aspects of the business and social world.

The reason for including military science and tactics is to give the student an opportunity to be prepared to help his country in a crisis such as we had December 7, 1941. The present success of the war is due largely to the many available reserve officers who were called to active duty. What the Ordnance Department has done is a fine example of how many reserve officers who had experience in research, engineering, and production work helped to take over almost similar functions in the military service to help make the United States the "arsenal of the world."

My own work as a maintenance specialist of antiaircraft artillery and fire-control equipment has been very broadening, but I was handicapped by the lack of previous instruction in the use of precision machine tools and welding.

The Army has been excellent experience to many of us lucky enough to be in a technical service, for we have had experiences in being the military supervisors of sections which are civilian in man power and in many ways of operation. It has been necessary to do maintenance in the field and to give advice and instruction to the men using the equipment, as well as to analyze and write maintenance procedures.

My contention is: let the college furnish the broad and well-rounded education; and either let the industrial concerns educate their engineer employees in the more specific electrical knowledge for their job or jobs or add a fifth semester of specialized study to the curriculums.

A man who is better able to see and meet the social and economic problems of today and who has an understanding of human nature will be a better man in his community and in his job. And, because he can do mechanical work with his hands, his opportunities in the field of hobbies are broader, and a man who has no spare time on his hands for loafing is a better and happier man.

H. L. ABBOTT (A '44)

(First lieutenant, Ordnance Department, United States Army, Philadelphia, Pa.)

NEW BOOKS • • •

The following new books are among those recently received from the publishers. Books designated ESL are available at the Engineering Societies Library; these and thousands of other technical books may be borrowed from the library by mail by AIEE members. The Institute assumes no responsibility for statements made in the following summaries, information for which is taken from the prefaces of the books. All inquiries relating to the purchase of any book reviewed in these columns should be addressed to the publisher of the book in question.

Reports on Progress in Physics, volume 9, 1942–43. Edited by W. B. Mann. Published by The Physical Society, 1 Lowther Gardens, Exhibition Road, London, S.W.7, England; printed by Taylor and Francis, Ltd., Red Lion Court, Fleet Street, London, E.C.4, 1943. 353 pages, illustrated, 10 by 7 inches, cloth, $6. (ESL.)

The new volume of this valuable series covers two years, instead of one, because of the need for economy. It includes ten reports on advances in our knowledge of atmospheric physics and chemistry and reports on other important fields, such as solar physics, the structure of rubber, counter tubes, field emission of electrons, physics and the search for oil, sound, crystal dynamics and X-ray scattering, and the physics of paintings. As usual, the articles have useful bibliographies.

Thorpe's Dictionary of Applied Chemistry, volume VI. By J. F. Thorpe and M. A. Whiteley. Fourth edition revised and enlarged with an index to volumes I–VI by J. N. Goldsmith. Longmans, Green and Company, London, England; New York, N. Y.; Toronto, Ont., 1943. 611 pages, illustrated, 9½ by 6 inches, cloth, $30. (ESL.)

Volume VI of the new edition of this

well-known reference work follows the pattern of the earlier ones. In addition to a wide variety of topics treated succinctly in dictionary style, it includes comprehensive articles on glazes and frits, beryllium, glue, glycerin, gold, hafnium, hydrogenation, helium, homogeneous catalysis, hydrogen, and gum inhibitors.

Twentieth Century Engineering. By C. H. S. Tupholme. Philosophical Library, New York, N. Y., 1944. 201 pages, illustrated, 9 by 5½ inches, cloth, $3. (ESL.)

Intended to give the layman an account of "some of the more spectacular engineering progress during recent years," this work deals with developments in mechanical power, workshop processes, air conditioning, refrigeration, chemical and metallurgical engineering, electrical engineering, traction, marine engineering, aircraft, and physics. The book is an American reprint of one published in England in 1942.

Essentials of Precision Inspection. By W. Mollard. McGraw-Hill Book Company, Inc., New York, N. Y., and London, England, 1944. 207 pages, illustrated, 11 by 8½ inches, cloth, $3. (ESL.)

This manual offers a systematic course of instruction in the use of the instruments employed in interchangeable manufacturing. These instruments are described; methods of checking their accuracy given; and their uses for various purposes explained and illustrated by problems.

Theoretical Chemistry. By S. Glasstone. D. Van Nostrand Company, New York, N. Y., 1944. 515 pages, illustrated, 9½ by 6 inches, leather, $5. (ESL.)

As the subtitle indicates, this book is concerned with those aspects of quantum mechanics, statistical mechanics, and molecular spectra that have a bearing on chemical problems. It is intended to provide an introduction that will enable the reader to understand clearly how quantum mechanics and statistical mechanics have been used in chemical work, and also to provide a foundation for further study.

Plastic Molds. By G. B. Thayer. Second edition. American Industrial Publishers, Cleveland 6, Ohio, 1944. 136 pages, illustrated, 9¼ by 6 inches, cloth, $3.50. (ESL.)

An experienced engineer here presents his ideas on the design and construction of molds. The various classes of molds used in compression and injection molding are described, and detailed description of various molds given. New material has been added to this edition, including contributions by several collaborators.

Bibliography and Abstracts on Electrical Contacts. Prepared by committee *B*-4 on electrical-heating, electrical-resistance, and electric-furnace alloys. American Society for Testing Materials, 26 South Broad Street, Philadelphia, 1944. 137 pages, tables, 9½ by 6 inches, cloth, $5. (ESL.)

This bibliography lists and, for most entries, abstracts over 800 publications on electric contacts. The arrangement is chronological, but subject and author indexes are provided. Among the subjects included are electric contacts in general, contact materials, circuit-breaker design, contact resistance and temperature, the electric arc, spark and glow discharges, contact wear, and circuit parameters as applied to contact operations.

ASTM Standards on Electrical Insulating Materials. Prepared by ASTM committee *D*-9 on electrical insulating materials; specifications, methods of testing, February 1944. American Society for Testing Materials, 260 South Broad Street, Philadelphia 2, Pa., 1944. 502 pages, illustrated, 9 by 6 inches, paper, $2.75. (ESL.)

This pamphlet contains the latest report of the committee on insulating materials, which proposes a routine method for testing the gas content of insulating oil and methods of testing askarels, and discusses the significance of tests of electrical insulating materials. It also contains the methods developed by the Society for testing varnishes and lacquers, molded insulating materials, plates, tubes, mineral oils, ceramic products, rubber, mica, and other insulating materials.

Nobel—Prize Donor. By M. Evlanoff. Published by the Blakiston Company, Philadelphia, Pa.; distributed by Fleming H. Revell Company, New York 10, N. Y.; London, England; and Edinburgh, Scotland, 1943. 190 pages, illustrated, 8¼ by 5¼ inches, cloth, $2.50. (ESL.)

This is the history of the Nobels, father and sons. The major part of the book is devoted to Alfred Nobel, the inventor of dynamite and creator of the Nobel prize. Attention also is given to the less known work of his brothers, Robert and Ludwig, and the immense oil industry which they founded in the Caucasus.

Pipeline to Battle. By P. W. Rainier. Random House, Inc., New York, N. Y., 1943. 302 pages, maps, 8½ by 5½ inches, cloth, $2.50. (ESL.)

The author of this interesting account of the African campaign of the British Eighth Army is an experienced mining engineer who joined the Royal Engineers in 1940 and served through the African campaign. His major task was to get water to the men in the front lines, and he laid pipe lines, drilled wells, and built waterworks throughout the campaign. His account of desert warfare is a vivid readable one, mostly written in the field.

System of Mineralogy. James Dwight Dana and Edward Salisbury Dana, Yale University 1837–1892, seventh edition, entirely rewritten and greatly enlarged. By C. Palache, H. Berman, and C. Frondel. Volume I, Elements, Sulfides, Sulfosalts, Oxides. John Wiley and Sons, Inc., New York, N. Y.; Chapman and Hall, London, England, 1944. 834 pages, illustrated, 9¼ by 6 inches, cloth, $10. (ESL.)

The new edition of this classic reference book will be welcomed by all students of mineralogy. As now issued, it is essentially a new book. Among the improvements are a new system of classification, a revision of morphological elements, a new way of presenting crystallographic data, introduction of optical characters, and other changes occasioned by the advances in the science during the last 50 years.

Control of Germany and Japan. By H. G. Moulton and L. Marlio. Brookings Institution, Washington, D. C., 1944. 116 pages, tables, maps, 9¼ by 6 inches, cloth, $2. (ESL.)

This new publication of the Brookings Institution is concerned with the problem of preventing future military aggression by Germany and Japan. The possibilities of their control by various economic measures, such as restriction of the importation of raw materials and the control of key industries are discussed, and the disadvantages of various plans are noted. A military control plan is described, which offers possibilities of success and which the authors believe to be the only effective means of checking aggression.

PAMPHLETS

The following recently issued pamphlets may be of interest to readers of "Electrical Engineering." All inquiries should be addressed to the issuers.

Organo-Silicon Films. By Francis J. Norton. General Electric Company, Schenectady, N. Y., 11 pages.

Second New England Traffic Engineering Conference. National Conservation Bureau, 60 John Street, New York 7, N. Y., 74 pages.

Simplified Spanish. Pan American Society, Box 315, Quito, Ecuador, South America, no charge.

Glossary of Foreign Welding Terms. American Welding Society, 33 West 39th Street, New York 18, N. Y., 16 pages, 50 cents.

Standards for the Installation and Operation of Centrifugal Fire Pumps. National Board of Fire Underwriters, 85 John Street, New York 7, N. Y., 52 pages.

The Financial Record of the Electric Utility Industry, 1937–43. Federal Power Commission, Washington, D. C., 10 pages, no charge.

Federal Agencies. Citizens National Committee, Inc., 1409 L Street, NW, Washington 5, D. C., 68 pages, 25 cents.

Postwar Planning in the United States. Twentieth Century Fund, 330 West 42nd Street, New York 18, N. Y., 151 pages, $1.

Good Public Relations for the General Contractor. Associated General Contractors of America, Inc., 10 Rockefeller Plaza, New York 20, N. Y., 24 pages.

Facilities Expansion. Bureau of Census, Washington 25, D. C.

Multiple Industrial Tenancy. Smaller War Plants Corporation, Washington, D. C., 18 pages.

Republic Goes to War. Republic Steel Corporation, Cleveland 1, Ohio, 94 pages.

V-Belt Handbook for Industrial Applications. The B. F. Goodrich Company, Akron, Ohio, 74 pages.

Business Engineering in the Shipyards, 8 pages. **Production Weapons,** 8 pages. George S. May Business Foundation, 11 South Dearborn Street, Chicago, Ill.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

A Graphical Analysis of the Voltage and Current Wave Forms of Controlled Rectifier Circuits

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Synopsis: Controlled rectifiers employing gaseous discharge tubes are used more and more frequently in the industrial field owing to the ease of control of the output voltage and the high conversion efficiency. The voltage and current wave forms at the different parts of controlled rectifier circuits are often non-sinusoidal and discontinuous due to the one-way valve-action of the rectifying elements. This paper presents a graphical method of constructing the voltage and current wave forms based on a few well-known fundamental principles of electrical engineering. This method is presented step by step and a few typical examples are shown to illustrate its validity.

1. Introduction

CONTROLLED rectifiers, either thyratron-tube or ignitron-tube equipments, are becoming more and more frequently used to obtain regulated sources of d-c from an a-c supply, both for d-c power requirements and for control purposes.

When the regulated source of d-c requires only a few kilowatts of power, the single-phase rectifier circuit is preferable because of its economical use of tubes and associated apparatus and because of the simplicity of its control circuits. In addition, the single-phase circuit is probably easier for the beginner to understand, and once its fundamental principles of analysis are mastered they can be extended readily to polyphase rectifier circuits.

Comparatively little material is available in current literature on the subject of controlled rectifiers, and in particular on the analysis of voltage and current wave forms at different parts of the circuit. Rectifier-circuit analysis is difficult because nonsinusoidal wave forms, which often involve discontinuities, are produced by the one-way controlled-valve action of the gaseous-discharge-tube rectifying elements.

A knowledge of these voltage and current wave forms is necessary to predict the nature of the output which can be obtained from a particular circuit and load combination, and to select elements of the correct ratings for the voltages and currents encountered. Furthermore, a mathematical analysis of these circuits presupposes a physical picture of the circuit behavior.

In order to show the significance of voltage and current wave forms with respect to each circuit element, consider the single-tube rectifier supplying a load comprising an inductance in series with a resistance as shown in Figure 1a. Such a load is typical of the field winding of a dynamo-electric machine, the coil of a contactor or relay, and the windings of clutches and solenoids. Figure 1b shows the instantaneous distribution of voltage across the four elements of the circuit, namely, anode-transformer secondary winding, rectifying element, inductance, and resistance.

Figure 2a to e, inclusive, shows the wave forms of voltage and current at the individual elements of the circuit of Figure 1. Figure 2b is the wave form of the output voltage of this half-wave rectifier circuit. In practice, as in the case of a coil whose inductance has inevitable resistance, the voltage across L cannot be segregated from the voltage across R so that each voltage can be observed separately. It is from the picture of Figure 2b that the wave forms of Figure 2c, d, and e are most readily derived.

Table I summarizes the significance of each picture of Figure 2 with respect to those features which are important to each circuit element.

In the presentation which follows, each of the fundamental principles is stated and the examples are analyzed by the application of these principles. The analysis is based on the following assumptions:

A. Linear circuit elements, *R–L–C*.

B. Transformer leakage reactance and resistance and the internal resistance and reactance of a counter electromotive force are neglected.

C. The solution is that of the steady state, although this method can be extended to transient analysis as is shown by the example of Figure 14a.

2. Fundamentals of Analysis

The several fundamental principles of circuit analysis which will be used throughout this discussion are stated herewith. Each principle is given a number by which it is referred to in the examples which follow.

2.1. Kirchhoff's Laws

2.11. *Voltage.* The summation of the instantaneous voltages around a closed circuit is equal to zero.

Referring to Figure 1b, it is shown that the instantaneous transformer voltage is equal to the sum of the respective voltages, at that same instant, across the tube, the inductance, and the resistance.

2.12. *Current.* The summation of the instantaneous currents at a given point equals zero.

2.2. The Resistor—Its Voltage-Current Relations

A pure resistor cannot store energy; it can only dissipate energy.

The energy consumed by a resistor is dissipated in the form of heat. This energy is expressed in watt-seconds, or joules, if e_R is in volts and i in amperes according to the relation:

$$\text{Energy} = \int e_R i \, dt$$

The power consumed by a resistor, or, the rate of energy dissipation, is equal to

Paper 44-75, recommended by the AIEE committee on electrical machinery for presentation at the AIEE North Eastern District technical meeting, Boston, Mass., April 19–20, 1944. Manuscript submitted February 21, 1944; made available for printing March 8, 1944.

P. T. Chin and E. E. Moyer are in the electronics section, industrial control engineering division, General Electric Company, Schenectady, N. Y.

$e_R i$ and is measured in watts if e is expressed in volts and i in amperes.

2.21. Both the current flowing through a resistor and the voltage across the resistor can be changed instantaneously.

2.22. The voltage drop across a resistor is always of such polarity as to oppose the flow of current through the resistor.

2.23. In a resistor, the voltage and the current have the following relation:

$$e_R = R\,i$$

2.3. The Inductor—Its Voltage-Current Relations

A pure inductor cannot dissipate energy.

Energy can be stored in the electromagnetic field of an inductor so long as current is flowing in the winding. This energy is expressed in watt-seconds if the inductance is in henrys and the current in amperes according to the relation:

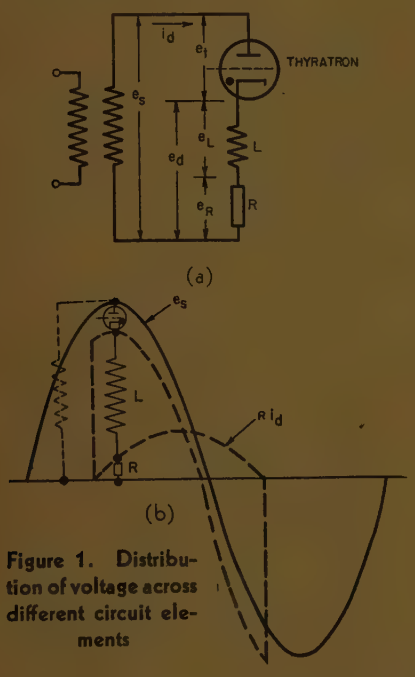


Figure 1. Distribution of voltage across different circuit elements

$$\text{Energy} = {}^1/_2 L i^2$$

Thus, in a given inductance of L henrys, the square of the current at any instant is a measure of the energy stored therein.

2.31. The energy stored in an inductor cannot be changed instantaneously; therefore, the current through the inductor cannot be changed instantaneously.

The voltage across an inductor can be changed instantaneously (a change of voltage is not a change of energy).

2.32. *Lenz's Law.* The polarity of the induced voltage across an inductor is always such as to oppose the change of current through the inductor.

That is, the polarity of the induced voltage cannot be determined from the direction of flow of current alone, but also depends on whether the current is increasing or decreasing. With the direction of flow of current as in Figure 3a, if the current is increasing the polarity of the induced voltage will be as shown in Figure 3b; if the current is decreasing the polarity will be as shown in Figure 3c; if the current is not changing there will be no induced voltage. The induced voltage is always equal and opposite to the impressed voltage.

2.33. In an inductor, the impressed voltage and the current have the following relation:

$$e_L = L\frac{di}{dt}$$

From this fundamental equation several useful conclusions can be drawn concerning the relation between the impressed voltage and the current of an inductor.

2.331. The slope of the current (di/dt in amperes per second) at any point equals the inductor voltage (volts) divided by the inductance (L henrys).

2.332. The current is either a maximum or a minimum when the inductor voltage is equal to zero.

Proof: i = maximum or minimum when $di/dt = 0$, that is, when $e_L = 0$

In Figure 4, the currents at t_2, t_6, t_{10}, t_{14} are maxima; the currents at t_4, t_8, t_{12} are minima. The voltages corresponding to these currents are equal to zero.

2.333. The current has a point of inflection when the voltage is either a maximum or a minimum.

Proof: If e_L = maximum or minimum, then the slope at that point must be either a maximum or a minimum.

In Figure 4, the current has a point of inflection at t_1, t_3, t_5, t_7, t_9, t_{11}, t_{13} and the inductance voltage has either a maximum or a minimum at these points.

2.334. The change of current from a value i_1 at a time t_1 to a value i_2 at a time t_2 is equal to the time integration of the inductance voltage between t_1 and t_2 (expressed in volt-seconds) divided by the inductance (expressed in henrys).

Proof:

$$\int_{i_1}^{i_2} di = \frac{1}{L}\int_{t_1}^{t_2} e_L\,dt$$

$$i\Big]_{i_1}^{i_2} = \frac{1}{L}\int_{t_1}^{t_2} e_L\,dt = i_2 - i_1$$

In Figure 4, the difference between the currents at t_2 and at t_6, that is, $i_6 - i_2$, is equal to

$$\frac{1}{L}(-A_2 + A_3)$$

2.3341. The current at any instant t_2 is equal to the time integration of the voltage divided by the inductance if the current equals zero at t_1.

Proof:

$$i_2 = \int_{t_1}^{t_2} e_L\,dt \text{ because } i_1 = 0$$

In Figure 4, $i_2 = \dfrac{A_1}{L}$ since $i_0 = 0$.

2.3342. If the current at t_1 is equal to the current at t_2, the time integration of the voltage during the interval between t_1 and t_2 must be equal to zero.

Proof: if $i_1 = i_2$ then

$$\frac{1}{L}\int_{t_1}^{t_2} e_L\,dt = i_2 - i_1 = 0$$

In Figure 4, if $i_{10} = i_{14}$, then $A_6 = A_7$.

2.335. The current wave is a smooth curve (no sudden change of slope) if there is no sudden change of voltage. The current wave is not a smooth curve (sudden change of slope) if there is a sudden change of voltage.

The current wave of Figure 4 is a smooth curve since there is no sudden change of voltage. The current wave of Figure 5 is not a smooth curve since there is a sudden change of voltage at t_3, t_6, t_9.

2.4. The Capacitor—Its Voltage-Current Relations

A pure capacitor cannot dissipate energy.

Energy can be stored in the electrostatic field of a capacitor so long as a potential is present across its terminals. This energy is expressed in watt-seconds if the

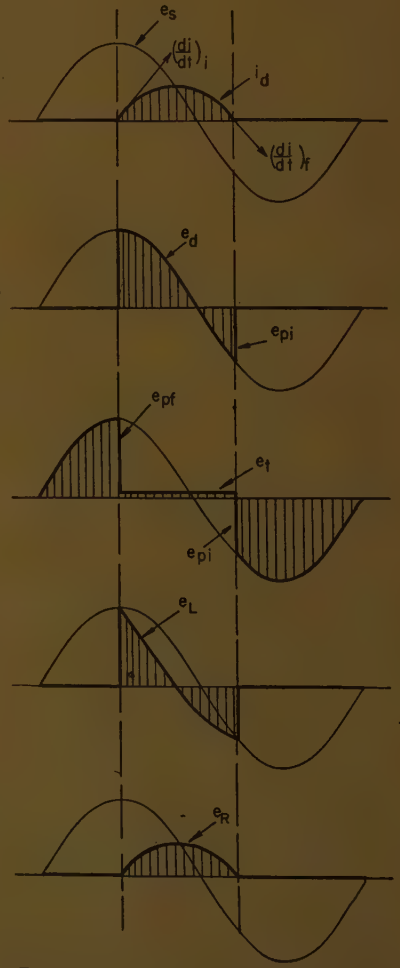


Figure 2. Voltage and current wave forms

Table I. Significant Features of Wave Forms of Figure 2 With Respect to Each Circuit Element

Figure	Tube	L	R	Transformer
2a	Peak current; Average current; Initial and final rate change of current	Average current	Rms current	Rms current
2b		Average voltage output (actually L and R are inseparably combined as in coil)		
2c	Peak inverse voltage conduction period			
2d		Peak voltage		
2e		Same as Figure 2a plotted in terms of iR drop		

capacitance is in farads and the potential in volts according to the relation:

$$\text{Energy} = {}^1/_2 C e_C^2$$

Thus, in a given capacitance of C farads, the square of the voltage at any instant is a measure of the energy stored therein.

2.41. The energy stored in a capacitor cannot be changed instantaneously; therefore, the potential across the capacitor cannot be changed instantaneously.

The current "through" a capacitor can be changed instantaneously (a change of current is not a change of energy).

2.42. The direction of flow of current in a capacitor cannot be determined from the polarity of the voltage across the capacitor alone, but also depends on whether the voltage is increasing or decreasing.

The voltage across the capacitor always changes in such a way as to oppose the flow of current through the capacitor. Therefore, in Figure 6b the current flows from "2" to "1" if the voltage is increasing; current flows from "1" to "2" if the voltage is decreasing; if the voltage is not changing there will be no current flow.

2.43. In a capacitor, the voltage and the current have the following relation:

$$= C\frac{de_C}{dt}$$

From this fundamental equation several useful conclusions can be drawn concerning the relation between the voltage and the current of a capacitor.

2.431. The slope of the voltage (de_C/dt, in volts per second) at any point equals the capacitor current (amperes) divided by the capacitance (C farads).

2.432. The voltage is either a maximum or a minimum when the current is equal to zero.

Proof: e_C=maximum or minimum when $de_C/dt=0$, that is, when $i=0$

In Figure 8, the voltage e_{C1} at t_1 is a maximum and the current i_1 at t_1 is equal to zero.

2.433. The voltage has a point of inflection when the current is either a maximum or a minimum.

Proof: If i=maximum or minimum, then the slope of the voltage must be either a maximum or a minimum.

In Figure 7, the voltage wave at t_1, t_4, t_7, t_{10} has a point of inflection when the current has maximum values at these corresponding instants.

2.434. The change of voltage from a value e_1 at a time t_1 to a value e_2 at a time t_2 is equal to the time integration of the capacitance current between t_1 and t_2 (expressed in ampere-seconds, or coulombs) divided by the capacitance (expressed in farads).

Proof:

$$\int_{e_C}^{e_{C2}} de_C = \frac{1}{C}\int_{t_1}^{t_2} i\,dt$$

$$e_C\Big]_{e_{C1}}^{e_{C2}} = \frac{1}{C}\int_{t_1}^{t_2} i\,dt = e_{C2} - e_{C1}$$

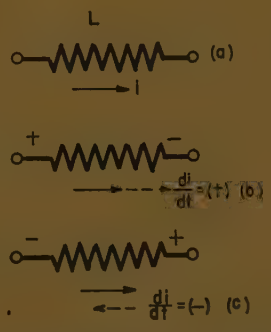


Figure 3. Determination of the polarity of the induced voltage in an inductor

In Figure 8, the difference between the voltages at t_0 and at t_3, that is, $e_{C3}-e_{C0}$, is equal to

$$\frac{1}{C}(+A_1 - A_2)$$

2.4341. The voltage at any instant t_2 is equal to the time integration of the current divided by the capacitance if the voltage equals zero at t_1.

Proof:

$$e_{C2} - e_{C1} = \frac{1}{C}\int_{t_1}^{t_2} i\,dt = e_{C2}$$

because $e_{C1}=0$.

In Figure 7, $e_{C1} = \dfrac{A_1}{C}$ since $e_{C0}=0$.

2.4342. If the voltage at t_1 is equal to the voltage at t_2, the time integration of the current during the interval between t_1 and t_2 must be equal to zero.

Proof: if $e_{C1}=e_{C2}$ then

$$\int_{t_1}^{t_2} i\,dt = e_{C2} - e_{C1} = 0.$$

In Figure 8, if $e_{C3}=e_{C6}$ then $A_3=A_4$.

2.435. The voltage wave is a smooth curve (no sudden change of slope) if there is no sudden change of current. The voltage wave is not a smooth curve (sudden change of slope) if there is a sudden change of current.

The voltage wave of Figure 7 is a smooth curve since there is no sudden change of current. The voltage wave of Figure 8 is not a smooth curve since there is a sudden change of current at t_0, t_3, t_6.

2.5. The Rectifying Element—Its Voltage-Current Relations

The rectifying element considered throughout this discussion is of the gaseous-discharge type—the thyratron or the ignitron.

Figure 4 (below). Graphs showing the relation between the voltage across and the current through an inductor



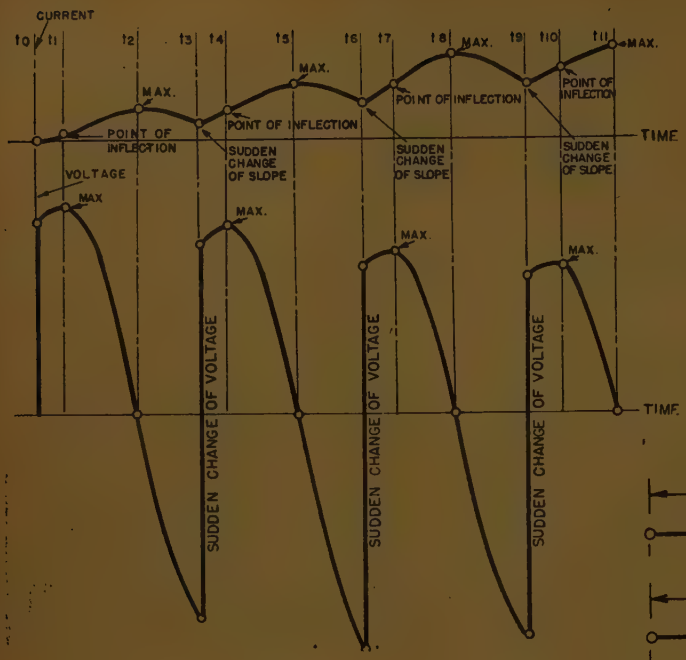
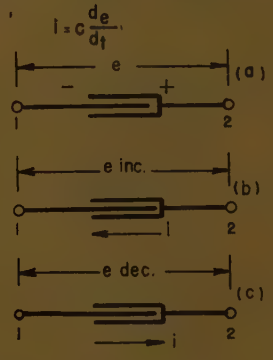


Figure 5. Graphs showing the relation between the voltage and current in an inductor (sudden change of voltage)

Figure 6 (below). Determination of the direction of current flow in a capacitor



2.51. Before the tube fires and before the current begins to flow, the tube acts as an open switch.

It is assumed that the striking voltage at which gaseous conduction begins is the arc-drop voltage (actually, the striking voltage may be more than arc-drop voltage,[2] say two or three times arc drop voltage). That is, it is assumed that gaseous conduction (as distinguished from electron space current before ionization occurs) begins as soon as the anode is positive with respect to the cathode by an amount equal to the arc drop.

In a thyratron, the actual firing point[3] is dependent upon anode voltage, grid voltage, and tube characteristic. In this discussion, the manner of controlling the firing point is incidental; only the results of the controlled tube as a circuit element are considered.

2.52. After the tube fires, the voltage across the tube is its arc drop which is practically independent of current. In this discussion, the arc drop during conduction is assumed to be constant.[4]

After the tube fires, the current through the tube is limited only by the combination of circuit elements in series with the tube, including any counter electromotive forces.

2.53. The voltage across the tube is equal to the voltage across the transformer minus the voltage across the load.

2.54. The gaseous discharge current through the tube ceases when the circuit causes the voltage across the tube to become less than the tube's arc drop. (In ignitron tubes without holding-anodes, the anode current ceases when the current falls below a certain minimum value necessary to maintain the cathode spot.)

2.6. The Load—Its Voltage-Current Relations

The load is a combination of R, L, C with or without a counter electromotive force.

2.61. The voltage across the load, when a tube is conducting, follows the supply-voltage wave-form (minus arc drop).

2.62. The voltage across the load, when no tube is conducting, depends on the nature of the load and whether a counter electromotive force or parallel load path is present. This is shown in Figures 9a, b, and c, respectively.

2.63. The relation between voltage and current at the load is complicated, but the approximate wave form of the current, in relation to the voltage, can be sketched by the application of these principles.

3. Examples

3.1. The pure inductance of L henrys (zero resistance) supplied from a single-tube, half-wave, controlled rectifier, fired at 60 degrees and having a peak anode voltage of E_{ps} volts This circuit is shown in Figure 10k.

Procedure

3.11. Sketch in the sine wave of secondary voltage e_s as in Figure 10a.

3.12. Then, the extinction point of the current will be 360—60 degrees since equal positive and negative areas of voltage must appear across the inductor whose current begins at zero and ends at zero. Figure 10b. Principle 2.3342.

3.13. Draw the voltage wave across the inductor. Figure 10c. Principle 2.6.

3.14. Sketch the inverse voltage of the tube. This is the transformer voltage minus inductor voltage. Figure 10d. Principle 2.53.

3.15. Determine the initial slope of the current wave. Principle 2.33.

This may be done graphically by constructing a right-angle triangle whose verti-

Figure 7. Graphs showing the relation between the voltage and the current in a capacitor (no sudden change of current)

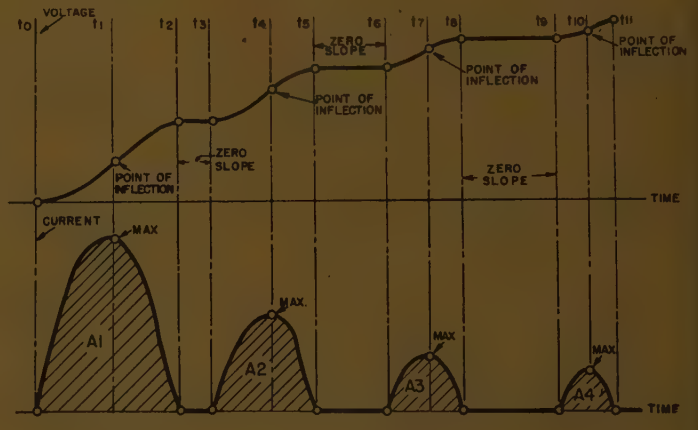
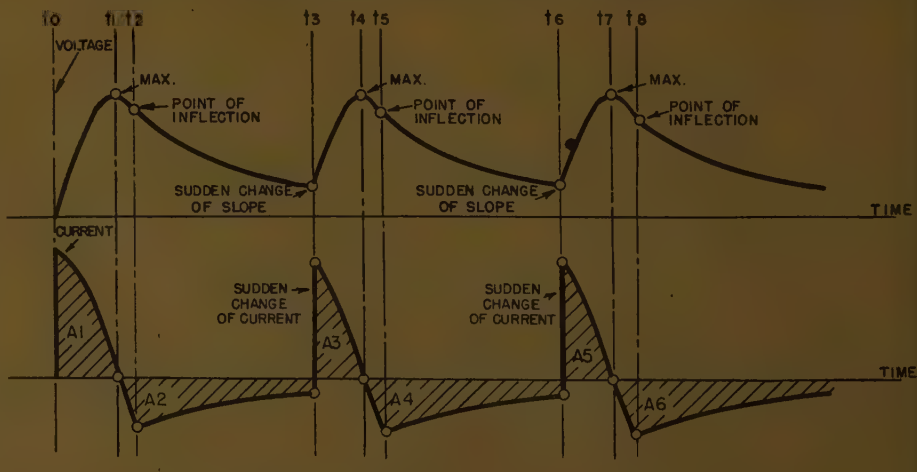


Figure 8 (below). Graphs showing the relation between the voltage and current in a capacitor



cal side is the voltage at this instant and whose horizontal side is L. Choose a convenient length for L, what length merely determines the scale of the current curve. Figure 10e. Principle 2.331.

3.16. Determine the final slope of the current wave in the same manner as for the initial slope. Figure 10e.* Principle 2.331.

3.17. Determine the peak value of the current wave (principle 2.3341) which occurs at the 180-degree point (principle 2.332) by calculating or estimating (for example, counting squares) the area underneath the positive half cycle of inductor voltage and dividing this area by the inductance. Figure 10f.

3.18. If necessary, additional points for the curve can be obtained by estimating the area up to a given point and dividing by L. Figure 10g and h. Principle 2.334.

3.19. The effect of arc drop can be determined from principle 2.52 by subtracting volts of arc drop from the inductor voltage. Figure 10i.

The peak current now occurs before the 180-degree point and this current wave is unsymmetrical, that is, the final slope differs from the initial slope. Figure 10j.

3.191. Figure 10l, m, and n show the voltage and current wave forms of a pure inductance supplied by a single-tube half-wave rectifier fired at different points.

3.2. An inductance of L henrys and a resistance of R ohms in series supplied from a single-tube half-wave controlled rectifier, fired at 60-degrees and having a peak anode voltage of E_{ps}, volts. This is the circuit of Figure 11h.

PROCEDURE†

3.21. Sketch in the sine wave of secondary voltage e_s as in Figure 11a.

3.22. Find the initial slope of the IR-voltage wave (which is proportional to the current). Principle 2.331. In this instance, the slope is constructed from the right-angle triangle whose vertical height is E_{pf}, volts and whose horizontal length is L/R seconds. Figure 11b.

3.23. Assume a peak value of IR voltage which peak value will fall along the positive-decaying-portion of the inductor voltage wave. Sketch in the IR-voltage wave, roughly. Its slope will be horizontal at the point of crossing the inductor voltage wave. Figure 11c. Principle 2.332.

3.24. Measure the area between the inductor-voltage curve and the IR-voltage curve. Divide this area (volt-seconds) by the time constant L/R (seconds) to determine the peak value of IR. If this calculated value of IR does not agree closely with the assumed value of 3.23 repeat 3.23 and 3.24 with a more accurate value until the value chosen in 3.23 and the value calculated in 3.24 agree closely. Figure 11d. Principle 2.334.

3.25. Find the extinction point by assuming a point such that the negative voltage area between the IR curve and the e_s curve equals the positive area of 3.24. Figure 11e. Principle 2.3342.

If the first assumption is not correct, repeat with more accurate values until the areas are equal.

3.26. If more accuracy is required, intermediate points can be drawn as shown in Figure 11f and g.

3.27. The inverse voltage across the tube can be drawn as in 3.14.

Find the average value of this voltage wave. This average will be a value which will give equal positive and negative shaded areas, as shown.

For current to be continuous, the end of the negative shaded area should coincide with the beginning of the successive positive area.

If the counter electromotive force is higher than this average voltage level, continuous current will be impossible. This is shown in Figure 12f.

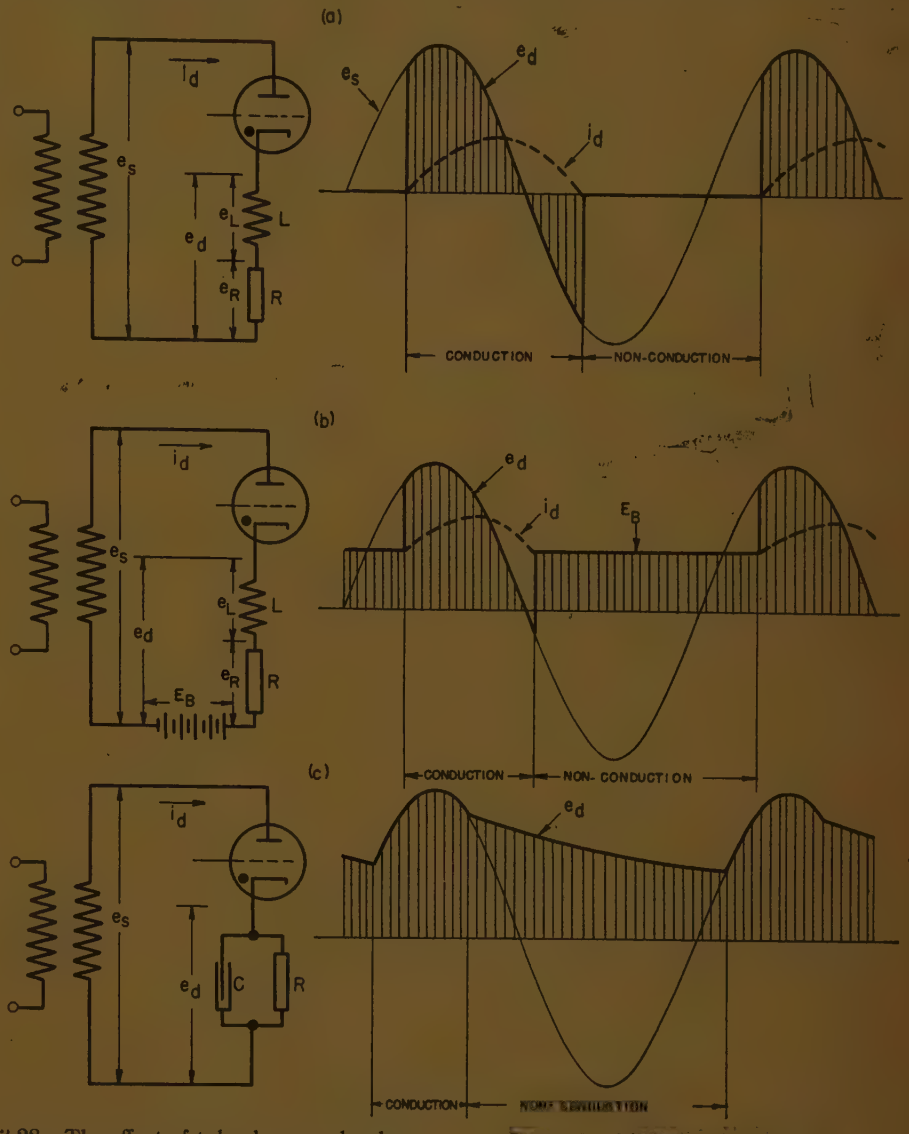


Figure 9. Voltage across the load

3.28. The effect of tube drop can be shown by subtracting arc drop from the inductor-voltage wave as in paragraph 3.19, then re-estimating the areas based on the new inductor-voltage wave.

3.3. The L–R load with counter electromotive force. Supplied from a two-tube biphase half-wave controlled rectifier, fired at 60 degrees and having an anode-to-neutral voltage of E_{ps}, volts. This circuit is shown in Figure 12g.

3.31. Sketch the transformer-voltage waves e_{s1} and e_{s2} and draw in the counter-electromotive force voltage to the same scale as in Figure 12a.

3.32. Test for current continuity as follows:

3.321. If the current is continuous, the total load voltage should be like that of Figure 12b.

3.322. If the current is discontinuous, the total load voltage should look like that of Figure 12f which has very definite regions of zero current when the output voltage is the counter electromotive force. Principle 2.62.

The average value of this voltage wave will be found to be higher than the counter-electromotive force level.

3.33. Assuming the discontinuous current case of paragraph 3.322, draw the transformer voltage waves and the counter electromotive force as in Figure 12a.

3.331. Draw the initial slope of the IR voltage wave as in Figure 12c.

3.332. Assume a peak value of the IR voltage wave. This peak value will fall along the

* In Figure 10e, the value of L was taken to be so many units of length. Each square of area is voltage height times a base width, so, the division of total squares of area by base divisions of L will give a measure of current.

† In the sketches which follow, the current is proportional to IR drop and is sketched in terms of IR drop.

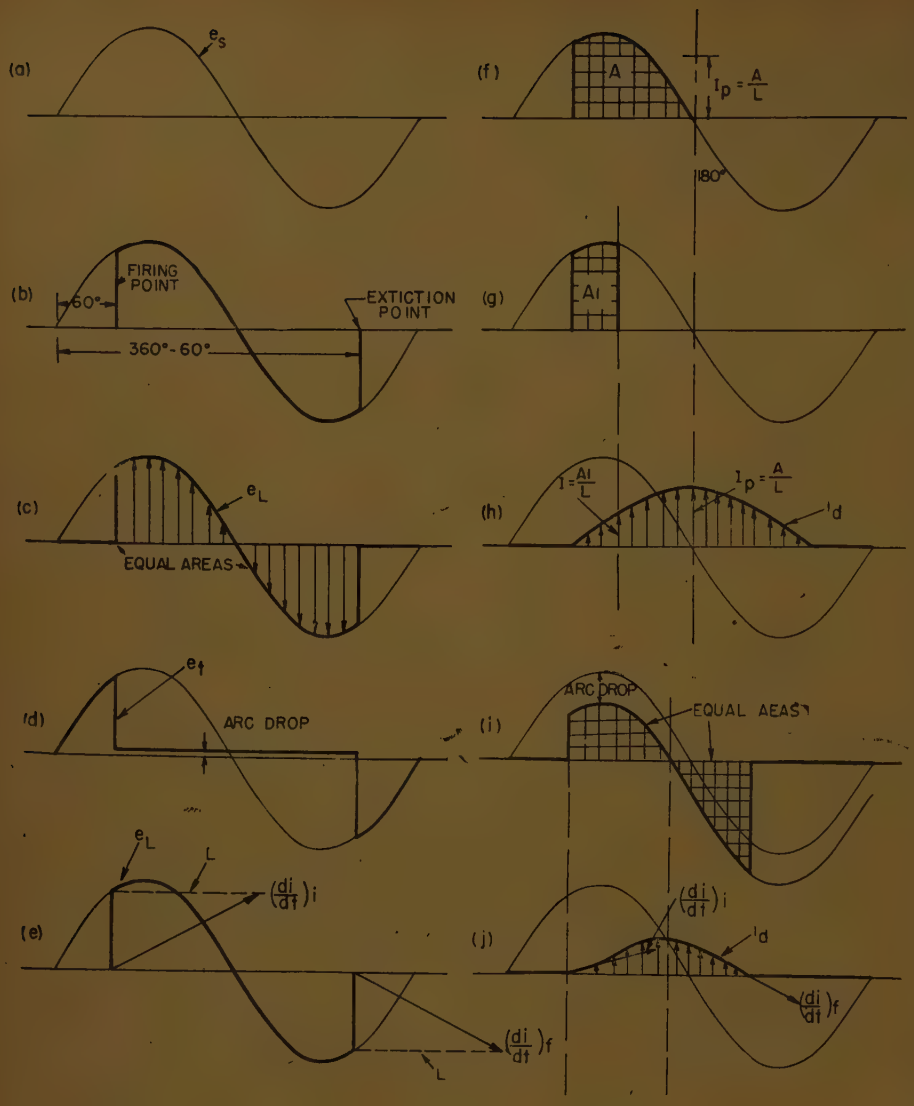


Figure 10a–j. Construction of the voltage and current wave forms of a pure inductance supplied by a single-phase half-wave controlled rectifier

decaying positive half cycle of the transformer-voltage wave and will have zero slope at the point of crossing this voltage wave. Sketch in the IR voltage wave as in Figure 12d.

3.333. Measure the area of inductor voltage and divide by L/R to determine the peak value of IR voltage at this point. If this calculated value of IR does not agree with the assumed value of 3.332, repeat 3.332 and 3.333 using an assumed value which is more accurate. Figure 12d.

3.334. Determine the extinction point of the IR voltage wave. This point will lie on the counter electromotive force line such that the negative inductor-voltage area will equal the positive inductor-voltage area as in Figure 12e.

If the first assumption of the extinction point is not sufficiently accurate, repeat with a more accurate value.

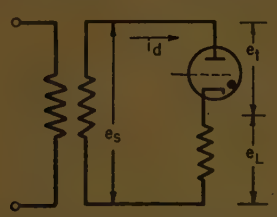


Figure 10k. Circuit diagram of a single-phase half-wave controlled rectifier supplying a purely inductive load

As a further refinement, intermediate points can be determined as in paragraph 3.26.

3.335. Check the final slope of the current wave as being the triangle formed by e_s and L/R.

3.336. If desired, the effect of tube drop can be found as in paragraph 3.19.

3.337. The tube inverse voltage can be found as in paragraph 3.14.

3.338. Find the average output voltage by averaging the counter electromotive force plus the IR drop.

3.34. Assuming the continuous-current case of paragraph 3.321, draw the transformer voltage waves, the firing point and corresponding average voltage line, the voltage across the load, and the counter electromotive force line as in Figure 13a.

3.341. Construct the IR drop about the average voltage line. This is done by assuming an initial value of IR drop which is approximately, but not exactly, midway between the average voltage line and the counter electromotive force line. This initial IR drop is therefore negative with respect to the average voltage line (that is, below the average voltage line) but positive with respect to the counter electromotive force line (that is, above the counter electromotive force line).

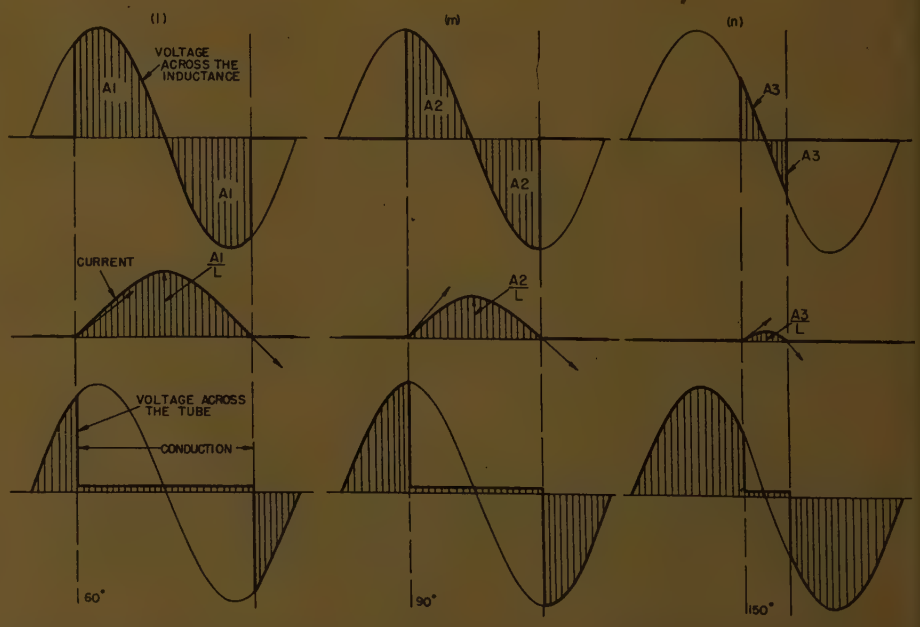
3.342. Draw the initial slope of the IR voltage wave based on this assumed initial value of IR drop. Figure 13b.

3.343. Assume a peak value of IR voltage which will fall along the positive-decaying portion of the transformer voltage wave and will have zero slope at the point of crossing this voltage wave.

Sketch in the IR voltage wave as in Figure 13b and c.

3.344. Measure the area of inductor voltage. Divide this area, A_1 in Figure 13d, by

Figure 10l–n. Voltage and current wave forms of a single-phase half-wave rectifier supplying a pure inductance firing at different points



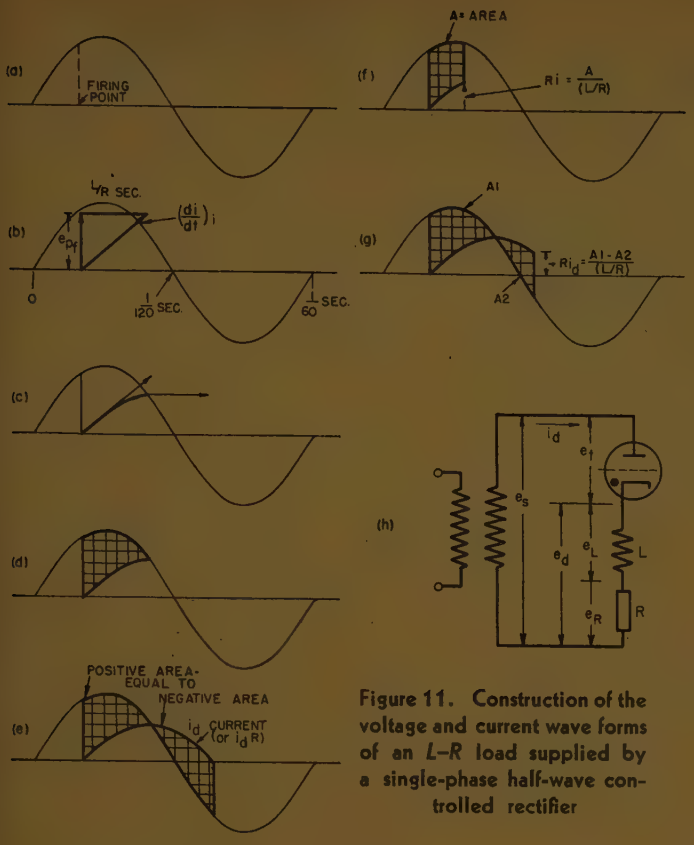


Figure 11. Construction of the voltage and current wave forms of an *L–R* load supplied by a single-phase half-wave controlled rectifier

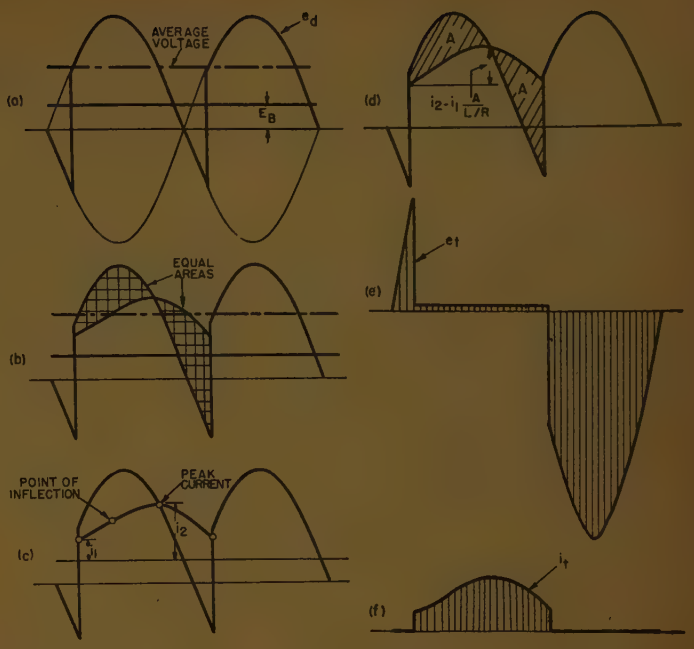


Figure 13. Construction of the voltage and current wave forms of *L*, *R*, and E_B in series supplied by a biphase half-wave controlled rectifier (continuous current)

L/R seconds to obtain the increased value of IR at this instant. The increased value of IR is measured from the level of the initial value.

If this estimated value of IR does not agree with the assumed value of paragraph 3.343, repeat the operation of paragraph 3.343 with a more accurate value.

3.345. The extinction point of IR drop must coincide with the initial value of IR drop as in paragraph 3.341.

Check that the positive and negative inductor-voltage areas are equal as in Figure 13d. Make repeated tries until sufficient correspondence is achieved.

3.346. Check the final slope of the IR curve by dividing the final IR drop by L/R.

3.347. As a check, the average value of the counter electromotive force plus IR drop should equal the average output voltage of the rectifier when fired at the assumed 60-degree point.

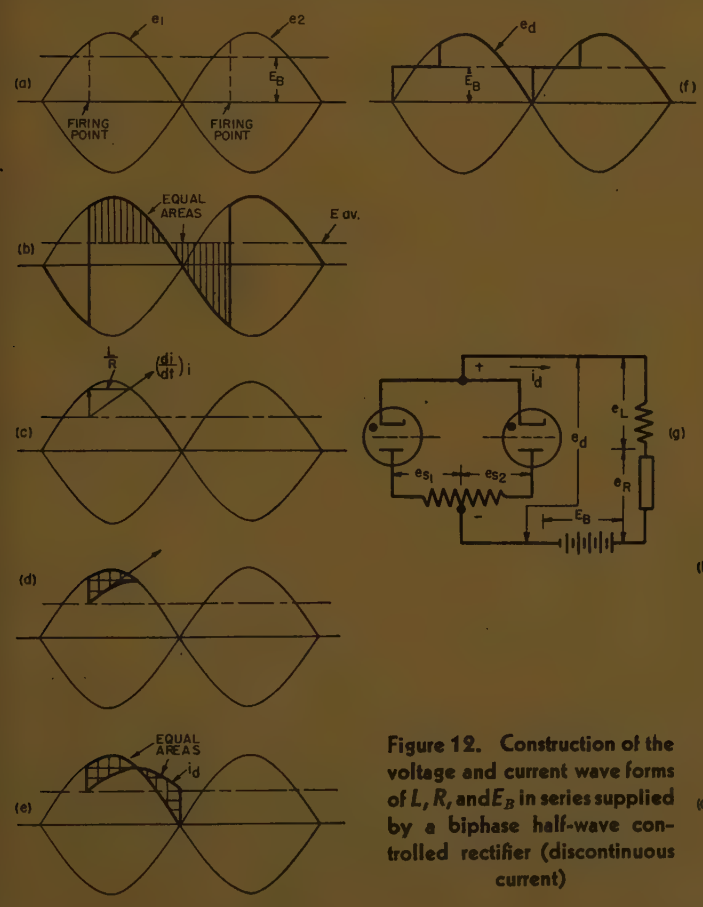


Figure 12. Construction of the voltage and current wave forms of *L*, *R*, and E_B in series supplied by a biphase half-wave controlled rectifier (discontinuous current)

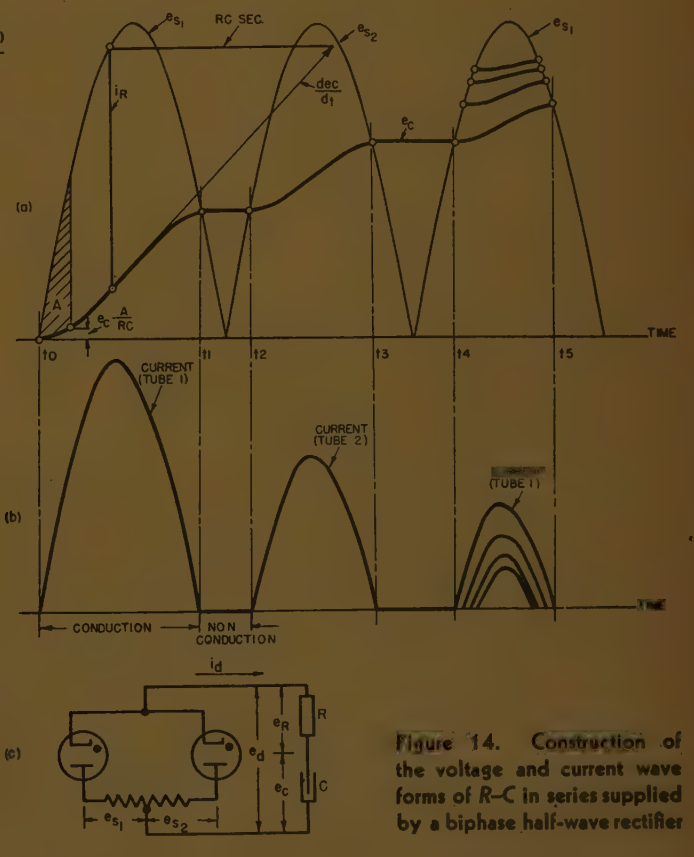


Figure 14. Construction of the voltage and current wave forms of *R–C* in series supplied by a biphase half-wave rectifier

3.348. As another check, the IR drop should have equal areas about the average voltage line.

3.349. The effect of tube drop can be considered as in paragraph 3.19.

Tube inverse voltage can be found as in Figure 13e.

Note the double inverse voltage wave form.

Tube current is shown in Figure 13f.

3.4. A capacitor of C farads in series with a resistor of R ohms and supplied from a two-tube bi-phase half-wave rectifier whose peak anode voltage is E_{ps} volts. This circuit is shown in Figure 14c.

Figure 14a shows the transient build-up of voltage across the capacitor when started from an initial, discharged condition.

3.41. Sketch the positive half-cycles of transformer voltages as in Figure 14a.

3.42. The initial slope of the voltage is zero since the initial current is zero. Principle 2.431.

3.43. Assume a capacitor voltage at the end of the first half-cycle of conduction at t_1, and sketch in the capacitor voltage wave.

3.44. Measure the area corresponding to the time integration of IR voltage. Divide this area (expressed in volt-seconds) by the time constant RC (expressed in seconds) to determine the capacitor voltage at t_1. It should check with the value assumed in paragraph 3.43; if not, repeat the procedure with a more accurate value in paragraph 3.43.

For more accurate results, a few intermediate points can be estimated by integrating the IR drop only up to these points.

3.45. At t_1 tube 1 ceases to conduct and tube 2 will not begin to conduct until e_{g2} is equal to e_{c1}. Starting from t_2, tube 2 will conduct. Assume a new increase of capacitor voltage as in paragraph 3.43 and repeat the procedure outlined in paragraph 3.44.

The build-up of capacitor voltage during the successive four half-cycles are shown in the third half-cycle of Figure 14a with the voltage wave superimposed on the same half-cycle.

3.46. The current wave forms can be drawn from Figure 14a as in Figure 14b.

4. Conclusions

A. The ability of predicting the voltage and current wave forms at different parts of controlled rectifier circuits is essential to a better understanding of these circuits.

B. By understanding a few basic relationships between the voltage and the current through the circuit elements—L, R, C, tube, and counter electromotive force—the voltage and current wave forms can be constructed graphically to a fairly accurate degree.

C. It is the intention of this paper to stimulate the general interest so that a more rigorous treatment of the controlled rectifier circuits will be made.

Appendix

Nomenclature

VOLTAGE

e_s—instantaneous voltage on the secondary side of the anode transformer

E_{ps}—peak value of the voltage on the secondary side of the anode transformer

e_R—instantaneous voltage across a resistor

e_L—instantaneous voltage across an inductor

e_C—instantaneous voltage across a capacitor

e_T—instantaneous voltage across the rectifier

e_d—instantaneous voltage across the load

E_B—battery voltage or counter electromotive force

e_{pf}—peak forward voltage

CURRENT

i_d—instantaneous current through the load

i_T—instantaneous current through the rectifier

$\left(\frac{di}{dt}\right)_i$—initial rate of change of current

$\left(\frac{di}{dt}\right)_f$—final rate of change of current

Design of Variac Transformers

EDUARD KARPLUS
MEMBER AIEE

MOST textbooks on transformers mention in a few concluding paragraphs that for certain applications special transformers can be used to good advantage. Foremost among these is the autotransformer which offers very important advantages if the ratio of input to output voltage is close to unity. Other special transformers mentioned are small instrument transformers, constant-current transformers, and voltage regulators. Voltage regulators are ordinarily divided into several groups: the automatic magnetic-flux-type regulator without moving parts, the induction regulator with one of its windings mounted so that it can be turned into different positions with respect to the other windings, and the tap-changing type, which requires special precautions if voltage has to be changed under load.

In power-distribution systems, a certain amount of flexibility is required to provide constant voltage to the ultimate consumer regardless of his distance from the station and regardless of load, and some feeder lines are connected to individual hand-operated or automatic regulators. The regulators are ordinarily tap-changing autotransformers or boosters, or they are induction regulators. While variable-ratio transformers of high rating have been manufactured for these applications by practically every one of the large transformer companies, smaller units have not been available until recently. It is the purpose of this paper to describe a variable-ratio transformer[1] intended primarily for low power. When the first Variac, the continuously adjustable variable-ratio autotransformer, was introduced in 1933, it found immediate acceptance. Today close to one million transformers of this type are in use. They are available from several manufacturers with ratings from 100 to 10,000 volt-amperes. Three transformers, as manufactured by General Radio Company, Cambridge, Mass., are shown in Figures 1–3.

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The analysis of optimum contact resistance in the equivalent contact circuit has been contributed by Doctor W. N. Tuttle.

Description

As the name Variac implies, the new transformer provides "variable a-c voltage" when connected to a power line. The basic principle is that of a tap-changing autotransformer. The tap-changing mechanism is the essential part of this transformer and the simplicity of this mechanism is responsible for its success. The "taps" are formed by exposed parts of the winding that is applied in a single layer over a toroidally shaped-iron core. The number of taps is the same as the number of turns of the transformer. Rotation of the control shaft causes a contact to traverse these turns and to tap off any desired part of the winding. The turns are closely spaced and the contact is wide enough to engage at least one turn

References

1. RECTIFIER-CIRCUIT DUTY, C. C. Herskind. AIEE TRANSACTIONS, volume 63, 1944, March section, pages 123–8.

2. RCA-2050 TUBE DATA, AVERAGE CONTROL CHARACTERISTICS. RCA Tube Handbook HB-3.

3. APPLIED ELECTRONICS (a book), Massachusetts Institute of Technology staff. John Wiley and Sons, 1943, pages 308–9.

4. MERCURY-ARC RECTIFIERS (a book), O. K. Marti and H. Winograd. McGraw-Hill Book Company, 1930, page 22.

at all times. It is possible, therefore, to move the contact under load without interrupting the circuit. To engage at least one turn at all times, the contact must be wider than the spacing between turns and it follows that turns can be short-circuited by the contact. To prevent damage to the winding, the contact is made of resistive material that limits the circulating current in the short-circuited turn. Since the load current has to flow through the contact, the resistance is kept as low as possible. The problem encountered is very similar to the problem of commutation in generators or motors, and the materials used for the contact are the same that are used in the familiar carbon brushes of rotating machines. The Variac transformer is designed to stand indefinitely the additional heat produced in the short-circuited turn and no attempt is made to prevent the contact from resting between turns. On the contrary, short-circuiting turns is one of the important advantages of the Variac, as it affords a gradual change from one tap to the other. Ordinarily, the voltage-per-turn is between one-half volt and one volt.

Uses

The advantages of the variable-ratio autotransformer over rheostats and other kinds of voltage-control devices are high efficiency, good regulation, and linear smooth control from zero to above line voltage. Mechanically the transformer is small in size and simple in construction.

In industrial control and in experimental problems in the laboratory, Variacs are used wherever alternating voltages must be adjusted, and by means of voltages, other quantities such as speed, heat, or light. Typical applications are speed control of motors, heat control of electric furnaces or heaters in a laboratory, illumination control in small theaters or dark rooms, output voltage control of high-voltage transformers and rectifier systems, filament control for radio-transmitter tubes, and voltage control for electrical or magnetic testing, calibration, and measurement.

Contact Problems

As mentioned before, the transformer uses a carbon brush, called the contact, to connect the load to the individual turns of the winding. Resistance characteristics, mechanical design, and thermal properties of the contact are most important for proper operation of the transformer. A schematic drawing of the Variac together with a detailed view of the contact is



Figure 1. 170-volt-ampere Variac transformer assembled for back-of-panel mounting

shown in Figure 4. The width of the contact is such that in sliding over the winding, one turn can be short-circuited but not two. The contact is represented as a block, connecting points 1 and 2 on successive turns of the winding to point 3 on the contact holder. The three-terminal block can be represented by an equivalent circuit of three resistors, R_1, R_2, R_3, between the points 1, 2, and 3 as shown in Figure 5. This equivalent circuit has been found useful in the following analysis of optimum contact design, but it should be understood that the contact resistance between a carbon brush and a metallic surface varies with the current passing through the surface and that the resistance of the carbon brush itself depends to a large degree on current density. Although the behavior of carbon brushes is not very well understood, they have been used for many purposes with great success for a long time and can be manufactured to close specifications. Experimental data show that the resistance of a carbon brush varies with the current passing through it in a way to maintain a substantially constant voltage drop. This voltage drop varies with the composition of the carbon and with the pressure applied and is used by brush manufacturers in the rating of their product. A typical current-voltage characteristic of a carbon brush is shown in Figure 6. It is reasonable to expect that a definite relation between this voltage drop and the voltage-per-turn of the Variac transformer has to be maintained to prevent excessive heating of the short-circuited turn and of the contact.

To show this relation, it is convenient to use the equivalent circuit shown in Figure 5. It can be seen that resistance R_3 is not included in the circuit of the short-circuited turn. Since it does not serve any useful purpose, it can be reduced to a small value by proper design of the contact holder and could be considered as part of the load resistance R_5.



Figure 2. An 860-volt-ampere Variac transformer for laboratory use



Figure 3. A five-kilovolt ampere Variac transformer

The resistance of one turn of the winding, R_4 is made as low as possible in accordance with transformer design practice. The resistances R_1 and R_2, therefore, have to be relied on to keep the circulating current in the short-circuited turn below a value that might damage the transformer or overheat the contact. This circulating current is shown as I_0 in Figure 5 and the load current as I_2. The voltage-per-turn between points 1 and 2 is e_0. To simplify the following analysis of optimum contact design, Figure 5 is drawn as a section of a two-circuit transformer rather than of an autotransformer, and the current in the winding preceding the short-circuited turn is the same as the load current. It is further assumed that the contact is symmetrically located with respect to the two points 1 and 2, but R_1 is not necessarily the same as R_2, since the current flowing through these two points will not be the same. In an autotransformer the current in the winding would be different from the load current, but the contact problem would be exactly the same.

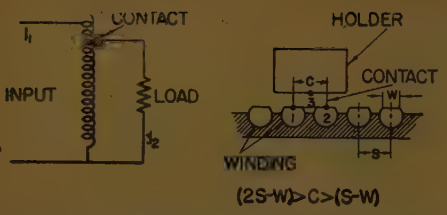


Figure 4. Schematic diagram of Variac transformer and contact

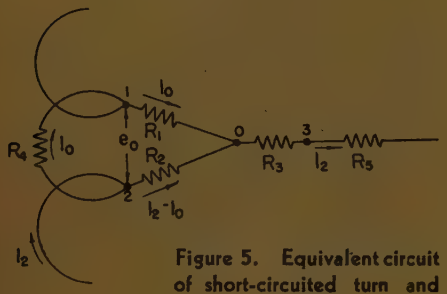


Figure 5. Equivalent circuit of short-circuited turn and contact

The problem of optimum contact design for a transformer of a given voltage-per-turn e_0 and given maximum load current I_2 is reduced to the problem now, of finding those values of R_1 and R_2 for which the energy loss is a minimum. It must be remembered, of course, that both R_1 and R_2 are the equivalent resistances of a single piece of carbon material and that they cannot be chosen independently of each other. The obvious suggestion, therefore, of making R_1 zero and R_2 infinite cannot be realized in a Variac transformer. It is a perfectly good solution and is used in tap-changing transformers of conventional design.

The energy loss in the two resistances according to Figure 2 is

$$W = R_1 I_0^2 + R_2 (I_2 - I_0)^2 \qquad (1)$$

and since

$$R_1 I_0 = R_2 (I_2 - I_0) + e_0 \qquad (2)$$

$$W = \frac{e_0^2}{R_1 + R_2} + I_2^2 \frac{R_1 R_2}{R_1 + R_2} \qquad (3)$$

The first term is seen to be the dissipation due to the voltage between adjacent turns acting across the two brush resistances in series. The second term is due to the load current flowing through the two resistances in parallel. The resistances R_1 and R_2 correspond to the currents I_0 and $I_2 - I_0$. If these resistances were constant and independent of current, the first term would be the no-load brush dissipation. If the ratio of the two resistances is called n^2

$$R_2 = n^2 R_1 \qquad (4)$$

and substitution of equation 4 in equation 3 yields

$$W = \frac{e_0^2}{(1+n^2)R_1} + I_2^2 R_1 \frac{n^2}{1+n^2} \qquad (5)$$

Setting the derivative of W with respect to R_1 in equation 5 to zero, the value of R_1 is obtained for which the contact loss is a minimum.

$$R_1 = \frac{e_0}{I_2 n} \qquad (6)$$

The combination of equations 4 and 6 gives a resistance value R_0 which might be called the optimum contact resistance.

$$R_0 = \sqrt{R_1 R_2} = \frac{e_0}{I_2} \qquad (7)$$

If the two brush resistances are equal, they should have the value R_0 for minimum total loss. If they are unequal, their geometric mean should have that value.

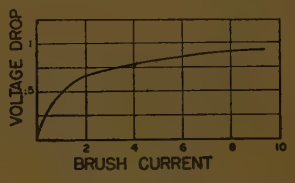


Figure 6. Current–voltage characteristic of a carbon contact

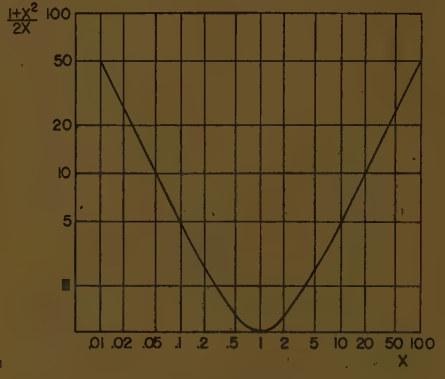


Figure 7. Plot of a function similar to that used in equation 11

To find out how much the contact loss is increased if the geometric mean of the two resistances deviates from R_0, a second parameter k is introduced

$$\sqrt{R_1 R_2} = n R_1 = k \frac{e_0}{I_2} \qquad (8)$$

and by substituting equation 8 in equation 5, the energy loss is found to be

$$W = e_0 I_2 \frac{n}{1+n^2} \cdot \frac{1+k^2}{k} \qquad (9)$$

If the two resistances R_1 and R_2 are equal and have the optimum value, both n and k are 1 and the energy loss may be called W_0.

$$W_0 = e_0 I_2 \qquad (10)$$

It follows that in the general case, if R_1 and R_2 have arbitrary values

$$W = W_0 \frac{2n}{1+n^2} \frac{1+k^2}{2k} \qquad (11)$$

Figure 7 shows the plot of a function similar to that used in equation 11 and it can be seen that the energy loss is increased by 25 per cent, for instance, if the geometric mean of the two brush resistances is double or is one-half of the optimum value, and that this increase of loss can be compensated by making one of the resistances four times as large as the other. It is apparent that to reduce the energy loss considerably below W_0, very high ratios of brush resistance values are required. The two factors in equation 11 tend to offset each other and consequently the geometric mean resistance must be held closely to the correct value.

Returning now again to Figure 5, it can be deduced without any computation that the energy loss $W_0 = e_0 I_2$ is obtained, if the voltage across R_1 equals e_0. In that case, the voltage across R_2 is zero. The full-load current I_2 flows through R_1 and the current in the short-circuited turn equals the load current. This condition is expressed by

$$I_0 = I_2 \qquad (12)$$

$$R_1 I_2 = e_0 \qquad (13)$$

Substituting equation 13 in equation 3, it is seen that

$$W = e_0 I_2 \qquad (14)$$

independent of R_2.

That a relation of this kind between the voltage per turn of a Variac transformer and the voltage drop in the contact would have to be maintained was suggested at the beginning of this analysis. It is apparent now that if the voltage drop in the contact equals the voltage per turn, the contact resistance is the optimum value according to equation 7, and the contact loss equals load current times voltage per turn.

Although it is possible, according to equation 11, further to decrease the contact loss if the two contact resistances R_1 and R_2 could be chosen at will, and could be maintained in the proper relation as the contact moves from one turn to the other, it has been found in practice that materials with the required characteristic are not available. As a matter of fact, it is often difficult to find a material that will fit the simple relation of equation 13 where resistance or voltage drop is specified for a single value of current.

If the maximum load current of a particular transformer is used to determine the optimum contact resistance in accordance with equation 13, the contact loss will be smaller than W from equation 14 for any other load current, and will be less than one-half this value for no-load condition. Under no load the short-circuit

current must flow through R_1 and R_2 in series, and each one will be larger than R_1 from equation 13. This follows from the current-voltage characteristic of the material used, which will always have the general shape shown in Figure 6. The no-load short-circuit current, therefore, will be less than one-half the maximum load current.

It has been shown in the preceding paragraphs that the contact loss is proportional to the load current and to the voltage-per-turn. If the voltage-per-turn were increased, as is usual in larger transformers, the contact loss would rise rapidly and would soon present a serious problem. Decreasing the number of turns below a definite value is undesirable for another reason. If a 200-turn Variac is adjusted for 50 per cent output voltage for instance, the voltage-per-turn at that particular point is 1 per cent of output voltage, but in a 100-turn Variac at 10 per cent output voltage, it is 10 per cent. It is true that the resistance of the Variac contact helps to smooth out the steps between turns, but it has been found in practice that 100 to 200 turns should be used, even in large transformers, to maintain smooth control at low output voltages.

At 100 turns the contact loss of a Variac transformer is 1 per cent of output power, according to equation 14. Iron and copper losses of the same transformer might be an additional 3 per cent. If operation of the transformer at rated output produces the maximum allowable temperature rise of winding and core, about an additional one third of the winding and core surface is needed to take care of the contact loss. The contact loss is concentrated in the small contact block and good heat conduction between the contact block and the cooling surface is required. The cooling surface is called the radiator.

Contact Design

Actual design of contact and radiator are closely dependent on the design of core and winding, but the basic requirements can be stated independently.

The active surface of the contact should be as large as possible to reduce current density and to spread the inevitable contact loss over the largest possible area. The width of the contact, shown as C in Figure 4, is usually very small, since it is determined by the cross section of the turns. The other dimension of the contact surface is related to the cross section of the core and depends on the length of turn available in the plane that has been chosen for operation of the contact. Figure 8 shows three different core cross sections. Core "A" would be best for a contact operating on top, and core "C" for a contact on the circumference. Small transformers ordinarily have banked windings. A core of shape "A," for instance, would be made with the outside diameter three times as large as the inside diameter. The winding would be applied to form a single closely spaced layer of turns on the outside, and the turns would pile up in three layers on the inside. The top surface, then, would not be available for operation of the contact, except at the outside edge.

Carbon brushes used on rotating machines are subjected to considerable wear and are made long, to avoid frequent replacements. Variac contacts show very little wear and are made as short as possible, to reduce losses and to afford better heat conduction into the radiator. In laboratory tests, contacts show extremely small wear in continuous operation. The wear seems to be independent of load, and is about 0.02 inch in one million adjustment cycles. In actual use, contacts wear more rapidly than this figure would indicate, and it appears that resistance to wear is decreased if the contact is allowed to rest under load in the same position for long periods of time. In any case, Variac brushes do not "wear in" after a few days of operation like brushes of rotating machines. Their performance can, therefore, be made more uniform and more reliable if they are subdivided into several independent units. Large Variacs are built with six or more contacts operating in parallel, but on small transformers, this added complication is not always justified.

Since Variac brushes do not wear in, current density and specific pressure vary within wide limits, and can hardly be compared with the values recommended by carbon manufacturers. Current carrying capacity and pressure of a given contact are best determined in statistical tests on the winding surface of the transformer.

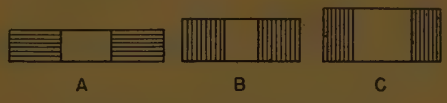


Figure 8. Cross section of cores

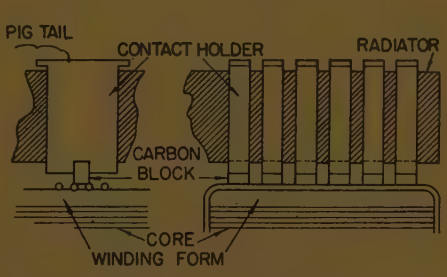


Figure 9. Six-brush contact operating on top of transformer

As an example, a 115 volt 5 kva transformer with 160 turns uses six contacts in parallel, each one carrying from five to ten amperes and having a spring pressure of about ten ounces. The dimensions of each one of the six blocks are approximately one quarter inch by one quarter inch by one quarter inch. These small carbon blocks are permanently fastened by soldering, or by pressure fit, into larger contact holders and form together with them six complete brushes. The brushes, in turn, are inserted into six slots of the radiator, and are pressed against the winding by springs. A schematic view of a six-brush contact is shown in Figure 9. The advantage of this brush construction, compared to the large carbon blocks used in motors and generators, is improved heat conduction to the radiator. As a result, the temperature of the contact surface can be kept at a lower value, and damage of the winding insulation is prevented. The radiator absorbs the heat of the contact primarily by conduction and transmits it to the surrounding air by radiation and convection. The material of the radiator is chosen for good heat conduction. Size and surface treatment determine radiation. Convection currents depend on general shape and are improved by ventilating channels.

To prevent the metal contact holders from short-circuiting the winding, if a carbon contact block should wear or break by accident, the travel of the brushes within the radiator slots is limited. Electrical connections to the brushes are made by flexible leads.

Contacts for Low-Power Transformers

The discussion of contacts, so far, has been mostly concerned with large transformers where contact losses must be reduced to a minimum. If contact losses rise rapidly with output power, it is equally true that they decrease on small transformers. On a 200-volt-ampere Variac transformer, for instance, with 500 turns, wound on a three inch-diameter core, contact losses according to equation 14 are 0.4 watt. This small power does not present any particular problem but the width of the contact would have to be under 0.025 inch to make it impossible to short-circuit more than one turn at a time. A carbon contact of that size would not be strong enough, but since the contact loss is low, no harm is done by

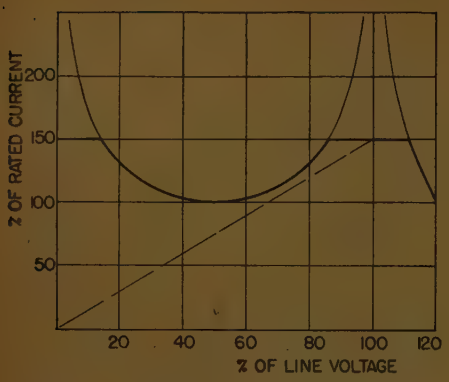


Figure 10. Output current for constant copper loss

increasing the width of the contact and letting it short-circuit several turns at a time. The transformer mentioned above actually uses a round contact of 0.09 inch diameter. It appears that six wires could be touched by a contact of this size, but because of the round shape, only three turns are effectively short-circuited and the contact loss is increased to 1.2 watts. It can be said in general that contact design need not follow the procedure outlined in the two preceding paragraphs unless it is desirable to limit contact losses to the inevitable minimum.

Winding and Core

A Variac transformer differs very markedly from a conventional transformer of equal rating. This is mainly due to four requirements discussed in detail in the preceding paragraphs: (1) the core is toroidally shaped, (2) the winding has to be applied to form a single layer at the contact surface, (3) the number of turns cannot be chosen for best efficiency or economy alone, and (4) the contact loss has to be added to the losses in copper and iron. If these points are kept in mind, winding and core can be designed by applying standard practice. In the following paragraphs, therefore, only a few points will be mentioned that are characteristic of the Variac.

A single-layer winding on a toroidal core does not fill up the winding space, and it is obvious that the amount of copper used is out of proportion to the amount of iron. This is particularly true with round wires and small toroids where the outside diameter must be much larger than the inside diameter. It is possible, of course, to use wire with rectangular cross section and to use large toroids, but that cannot be done when a compact transformer of low output power is desired. Rectangular wire could be used to great advantage in large transformers, over two kva for instance, but so far, the mechanical problem of applying the winding over the toroid has not been solved.

The resistance of a single-layer winding of round wire increases with the third power of the number of turns, and it follows, that the power rating of a toroidal core depends upon the voltage applied. For this reason Variac transformers cannot be built economically for high voltages.

The rating of a conventional transformer core increases rapidly with the frequency applied, since the number of turns can be decreased. In a Variac transformer, the number of turns is determined by the voltage applied and the only benefit obtained at increased frequencies is a reduction of core loss. However, if the cross section of the core is reduced at high frequencies, the gain is in the same proportion as in any transformer.

A laminated toroidally shaped core can be formed by two methods which are indicated in Figure 8. Core *A* is obtained by stacking ring-shaped laminations, core *B* by winding a long strip over a mandrel. Losses in core *B* will be slightly lower than losses in core *A*, but both methods are equally satisfactory, and the choice depends mainly on the availability of materials and tools.

Application of a winding over a toroidal core requires special equipment, unless it is done by hand. Hand-winding, however, is not very practical, since small transformers have many hundreds of turns, and large transformers require wires that are too hard to handle. Toroid-winding machines[2] are ingeniously designed and apply single-layer or banked windings on large and small cores. They can handle a very wide range of wire sizes. It takes about 5 minutes to wind 500 turns of number 28 wire on a 3-inch-diameter core, and 25 minutes to wind 160 turns of number 8 wire on a 12-inch-diameter core.

The wire used is always insulated. A very high grade of insulation is required to stand the mechanical abuse during the process of winding, and to stand high temperatures in operation. At the contact surface, the insulation has to be removed. In addition, the wire is flattened to offer a larger surface to the contact, as shown in Figure 4. Both operations can be done on a grinding wheel. It is very important that the wires cannot change their position after grinding, since that would destroy the contact surface. The wires, therefore, are usually embedded in a winding form with a groove for each individual turn.

Mounting of the toroidal core, covered almost entirely by the winding, is another problem that requires serious consideration. Ordinarily, the winding covers 320 degrees and clamps cannot be used where they would interfere with the movement of the contact, or where they would form a short-circuited turn around the core. Permanent close alignment of the control shaft and the contact surface is very important, and high local pressure must be avoided on the winding.

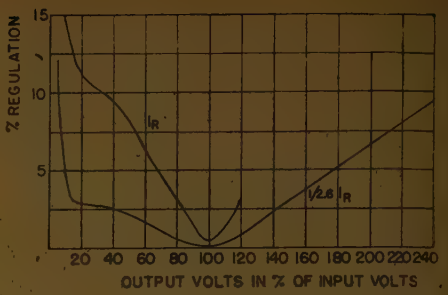


Figure 11. Regulation of transformer with 1.2 and 2.4 voltage step-up

Most Variac transformers are designed for air-cooling, but forced air draft and oil have been used in special applications to reduce size and weight to a minimum.

Rating and Taps

In the discussion of contact loss the term "maximum load current" has been used. It was assumed that the transformer would reach the highest permitted temperature at this current, the largest current that the contact has to handle. This current, called the "rated current," is an important characteristic of the Variac transformer.

Figure 4 shows the most commonly used connection of a Variac transformer to line and load. If the output voltage into the load is changed by moving the contact, and the load current is held constant by changing the load resistance, the current in the different parts of the winding changes. Copper losses, except those due to magnetizing current in the winding, start at zero for zero output voltage, reach a maximum at one-half line voltage, are zero at line voltage, and increase rapidly from there on. Core and contact losses are independent of output voltage.

The point above line voltage, for which the losses equal again the losses at half line voltage, is found to be at 20 per cent above line voltage. For this reason the line voltage tap on Variac transformers is almost always at 83 per cent of the turns. At rated current, the temperature rise of the Variac transformer reaches the maximum value at two settings of output voltage: at one-half line voltage and at maximum. The rated current could be exceeded at all other points if it were not

limited by the contact. In Figure 10 load current in per cent of rated current, for constant copper loss, is plotted against output voltage, in per cent of line voltage. If the contact is designed for a "maximum current," 50 per cent over "rated current," load currents up to the heavy line in Figure 10 can be used. The dotted line is the voltage-current characteristic of a constant load that draws "maximum current" at line voltage. It can be seen that this straight line is always below the limiting curve, and it follows that the transformer would not overheat at any output voltage, under line-voltage, if connected to this load. Experience has shown that contacts can be designed to carry considerably more than rated current in small Variac transformers, but in large transformers, the contact surface is not always sufficient to allow an increase above rated current as high as 50 per cent. Variac transformers are, therefore, rated by the following terms: (1) rated current —the current that can be drawn at any position of the control shaft; (2) maximum current—the current that can be drawn at low output voltages and at voltages near input voltage; (3) load rating —the volt-amperes obtained by multiplying maximum current by line voltage.

Whenever output voltages above line voltage are not required, or when the load could be damaged by overvoltage, the line should not be connected to the tap of the transformer, but to the full winding. In other applications it may be desirable to obtain more than a 20 per cent increase of output voltage with respect to line voltage, and the input could be connected still further down on the winding. The voltage across the full winding cannot be increased, of course, since that would increase flux density and voltage-per-turn, but all Variac transformers designed for 230-volt lines are tapped for connection to 115-volt circuits. If power is supplied at rated current to the center tap of a transformer, the output current is one half of rated current. If power is supplied to a tap that will give a maximum output voltage of $2 \times 1.2 = 2.4$ times input voltage, the connection provided on 230-volt Variac transformers, the output current for which copper losses are not excessive is 1/2.6 of rated current.

Regulation

Regulation of a transformer can be expressed as a voltage drop under load, in per cent of output voltage. In a Variac transformer the voltage drop in the contact is added to the voltage drop in the winding, and since the contact drop at full load current always equals the voltage-per-turn, the regulation at low output voltages is considerably increased. On the tenth turn of a transformer, the regulation due to contact drop always must be 10 per cent under these conditions, but the actual voltage of the tenth turn decreases as the total number of turns is increased. In a 200-turn Variac, connected to give 20 per cent overvoltage, for instance, the voltage-per-turn is only 0.6 per cent of input voltage, and contact regulation is better than 10 per cent, down to output voltages as low as six per cent of line voltage.

Autotransformers have inherently better regulation than two-circuit transformers. The improvement is most noticeable if the output voltage is close to line voltage and regulation is so good that careful protection against short circuits in the load is important. On the other hand, the leakage reactance of a single-layer winding on a toroidal core is large compared to the leakage reactance of a conventional transformer where it can be minimized by proper design.

Over-all regulation of Variac transformers in their most useful range, near input voltage, can be considered excellent compared to other control devices, but it should be kept in mind that at low output voltages and at high ratios of voltage step-up, voltage drop under load cannot be disregarded. A typical regulation curve is shown in Figure 11, where voltage drop in per cent of output voltage is plotted against output voltage in per cent of input voltage. The upper curve applies for a Variac transformer with a maximum voltage step-up of 1.2 at rated current and the lower curve for a voltage step-up of 2.4 at 1/2.6 of rated current. The effect of contact drop can be clearly seen in these curves at low output voltage. The inversion of the curves at one third of input voltage is due to the fact that output power decreases at lower output voltages. A further discussion of regulation is beyond the scope of this paper, but it is interesting to note that for constant voltage-per-turn the regulation at rated current varies very little with the size of the transformer.

Special Transformers

The use of a carbon composition contact on the turns of a transformer winding is not limited to toroidal autotransformers. This particular application has been discussed in detail, since it combines many advantages, but it is obvious that the transformer core could have any desired shape and that it could carry any number of additional windings.

One important application of a Variac transformer with an additional winding is a voltage regulator with limited range, that can vary the output voltage from 100 to 130 volts, for instance, when connected to a 115-volt line, or maintain constant 115-volt output voltage if the line voltage varies from 90 to 120 volts.

In another application, line-voltage is connected to the contact of a multiple-winding transformer. Moving the contact changes the number of primary turns and varies simultaneously the output voltage of several windings.

For applications in multiple-phase circuits two or more Variac transformers are mounted together with their contacts controlled by a single shaft.

References

1. United States patent number 2,009,013, issued July 23, 1935.

2. Toroidal Winding Machines, F. E. Planer. *Journal of Scientific Instruments*, Institute of Physics, London, volume 20, December 1943, pages 185–90.

Prevention of Rotor-Winding Deformation on Turbogenerators

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MEMBER AIEE

Synopsis: During recent years there have occurred several cases of failures on turbogenerators which were found to be due to a singular type of deformation of the rotor windings. It is recognized that the deformation of rotor windings is due to forces set up in the windings by the interaction of centrifugal force and temperature change.[1]

The purpose of this paper is to discuss briefly the phenomena responsible for this type of deformation, and to show that by means of preheating of the rotor windings to a definite predeterminable winding temperature, the resulting mechanical stresses can be very materially reduced so that winding deformation is held to a minimum.

Cause of Rotor-Winding Deformation

THE phenomenon of rotor winding deformation on turbogenerator rotors is discussed in reference 1. As copies of this paper are not readily available, the subject is here briefly reviewed.

All large turbogenerator rotors are built with the rotor winding, consisting of a number of multiturn coils laid in milled radial slots with metal wedges supporting the coils against the radially directed centrifugal force. Asbestos and mica are the principal insulating materials. At the ends of the rotor body, the individual coils are supported against deformation by end rings, with insulating material bracing blocks. The mechanical strength that can be built into the bracing is limited by the mechanical strength of the insulating materials used for coil insulation.

Several cases of rotor winding failure have occurred that were found to be due to a peculiar type of deformation of the rotor coils. This deformation is the result of shortening of the rotor conductors within the rotor slots. The failures, involving electrical grounds and interturn short circuits, all occurred at points where the rotor windings leave the rotor body or in the portions of the coils located under the end bells of the rotor. Grounds developed by crushing of the coil insulation, whereas interturn short circuits were found to be caused by the crushing of the turn-to-turn insulation. In each of these cases the underlying cause was a deformation of the coils, in which the conductors nearest to the center of the rotor shaft had become shortened most, while conductors more remote from the rotor shaft showed diminished shortening; the outermost conductors being found entirely unaffected. Blocking between coils was found either bent or broken, showing that the forces involved were high in order of magnitude. In some cases, the innermost conductors were found displaced from their original position by as much as the width of the conductors. These displaced conductors, under the influence of centrifugal force, were subjected to eccentric loading between conductors and the resulting high unit pressures on the turn-to-turn insulation ultimately produced structural failure of this insulation, causing turn-to-turn short circuits. Figures 1A, 1B, and 1C illustrate the conditions.

The basic cause of rotor-winding deformation may be explained if the processes of starting, loading, and shutting down of the turbogenerator are examined in detail. The turbogenerator, having cooled during a preceding outage to approximately ambient temperature, is started via the turbine and is driven at low speed for a period varying from one half to two hours, depending on the size of the unit involved and on the temperature the turbine has cooled to. Near the end of the turbine "warming up" period, the generator speed is rapidly increased to synchronous speed and, after applying field excitation, the generator is synchronized to the bus and loaded. The significant point of this starting procedure is that field current is not applied to the rotor winding until after nearly all of the centrifugal force is established by the rotational speed and the conductors are locked in position as a result. The rotor winding is therefore not subjected to a temperature rise until after the full effect of centrifugal force is established. After the field current is applied and as the generator is loaded, the rotor winding resistance loss appears as heat in the rotor conductors. As the temperature of the conductors increases a portion of this heat, proportional to the time rate of temperature rise, is stored in the thermal capacitance of the conductors and the remainder is transferred by conduction through the coil insulation to the rotor body at a rate proportional to the instantaneous temperature difference between the rotor winding and the rotor body. This process of combined heat storage and heat dissipation by conduction, results in an exponential approach to an ultimate temperature and involves a thermal time constant (Appendix IV). As the total weight, and with it the thermal mass, of the rotor winding is small, the thermal time constant of rotor-winding temperature rise over the rotor-body temperature is small. As heat is transferred to the rotor body, it experiences a temperature rise over the surrounding air. During increasing rotor-body temperature a portion of the heat transferred to it from the rotor winding is again stored (proportional to the rate of temperature rise) and the remainder is transferred to the surrounding air-gap air by forced convection at a rate proportional to the temperature difference. Because of the great weight of the rotor body, its thermal time constant is large (Appendix IV). With suddenly applied and thereafter constant field current the process of heat dissipation from the rotor winding is summarized as follows: The temperature rise of the rotor body over air-gap air (or approximately ambient air) reaches its final value slowly. After the initial fast temperature rise of the rotor winding, the winding continues to increase in temperature as the rotor body heats, finally maintaining, for constant field current, a constant differential.

While these temperature changes occur, the rotor conductors are under the influence of centrifugal force which tends to force them radially outward. Each rotor-conductor's centrifugal force is a function of its radial location and angular velocity. The centrifugal force of the innermost conductor is imposed on the next conductor, whose own centrifugal force adds to that of the first conductor, imposing the total on the third conductor and so on, until the wedge supports the accumulated centrifugal force of all conductors in the coil. Figure 2 shows a typical curve for a large rotor, giving centrifugal forces in terms of conductor location.

Any tendency of the coil as a whole, or of any conductor in the coil, to move in an axial direction is opposed by the frictional resistance created by the centrifugal force. The magnitude of the coefficient of friction is somewhat uncertain but probably

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lies between 0.1 and 0.3. Even with the lower value and with values of centrifugal force as given in Figure 2, it is obvious that large portions of the slot lengths of the rotor conductor are firmly locked on each other and on to the wedge.

However, as the rotor conductors are being heated they tend to expand against the frictional forces which hold them locked in place. When the conductor-temperature rise is low, the tendency of the conductors to expand results in the creation of compressive stresses in the conductor within the elastic limit of the conductor material. With increasing rotor-body temperature, additional stress is produced on account of the differences in the coefficients of linear expansion of rotor conductor and rotor body, which at some critical temperature rise produce total stresses in the rotor conductor in excess of the elastic limit, and result thereafter in permanent plastic deformation. When the generator's load is reduced, the temperature of the rotor winding decreases, and the initial stress, which is held in elastic deformation, is released. When the generator is shut down, cooling takes place without the restraint of centrifugal force and the rotor conductors are free to contract as they cool. The plastic strain which was initially established at the high temperatures is retained, leaving the so-strained conductors permanently shortened. Repetition of this cycle of starting, loading, and shutting down at sufficiently high temperature rises results in continuing deformation in spite of the hardening effect of the cyclic plastic working of the conductor material. At some lower temperature rises however, deformation may cease after a small number of cycles because of the hardening effect of plastic working during the first few cycles.



Figure 1A. Rotor-winding distortion, 160,000-kva 1,500-rpm generator

The preceding analysis gives a satisfactory qualitative exposition of the phenomenon of winding deformation for which a quantitative verification is more complex. The added complexity comes from the fact that in the discussion of conductor temperature an assumption of equal temperature for all conductors of a coil is really inadmissible. The temperature of the conductors of a coil is lowest for the outermost conductor, it rises to a maximum somewhere in the interior of the coil, and drops again toward the innermost conductor. Figure 3 shows a relation of conductor location and temperature rise, calculated by means of an equivalent electrical network for a rotor coil of a large generator neglecting all heat flow in the axial direction. Based on the temperature rise distribution of Figure 3, on an idealized stress-strain curve, as shown in Figure 4, and in consideration of the effects of varying centrifugal force with conductor location, the deformation for a single cycle, Figure 5, was calculated. Comparing calculated deformation (solid line in Figure 5, with the measured deformation on the generator rotor shown in Figure 1A (dashed line in Figure 5) good agreement may be observed to exist for all conductors excepting the two innermost ones. Similarly, calculated curves, shown as Figure 8 in reference 1, show a like tendency toward decreasing deformation for the innermost conductors. It appears, therefore, that in addition to the previously discussed factors, there must be others in order to account for the remaining discrepancy between calculated and observed conditions. One of these factors is probably buckling of the innermost conductors in whatever space there may exist at the bottom of the slot. The force required to buckle a conductor as a column may be expected to be relatively small since a conductor, as a column, is a very weak one indeed, even if the radial bracing by centrifugal force is taken into account.



Figure 1B. Rotor-winding distortion, 21,000-kva 1,500-rpm generator

Prevention of Rotor-Winding Deformation By Preheating

In the preceding section it was shown that the rotor-winding deformation is caused by the temperature change which occurs after the rotor conductors are prevented from free expansion by the locking action of centrifugal force. It was shown that when the temperature changes are sufficiently small, deformation is small and moreover may be expected to cease entirely after a limited number of cycles. The prevention of excessive rotor-winding deformation, therefore, lies in the reduction of the magnitude of temperature variations. The permissible magnitude of temperature variation is a function of the mechanical properties of the conductor material and the generator designer has control of both the magnitude of temperature variation and the conductor material used. However, the operator gains control of one of the factors involving the magnitude of stress developed in the conductor, when he is enabled to fix the initial temperature from which all subsequent temperature changes take place. By preheating the rotor winding and body with the body revolving at very low speed the expansion of the conductors and rotor body, as the temperature rises from ambient to preheat temperature, takes place free from frictional restraint produced by centrifugal force. Subsequent temperature variations due to generator load changes at synchronous speed then produce stresses

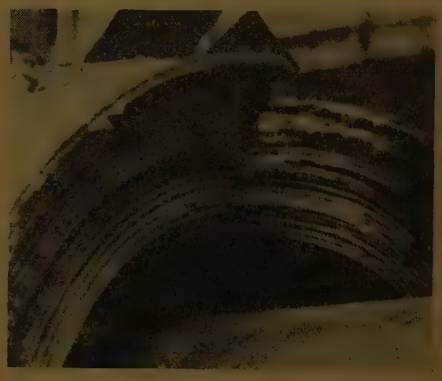


Figure 1C. Rotor-winding distortion caused by overloading 50,000-kva 50-cycle generator

which are proportional to the difference between actual and preheat temperature instead of the difference between actual and ambient temperature. Furthermore, the initial or preheat temperature can be chosen in such a manner that the stress in the conductor material becomes a tensile stress at light loads and the compressive stress which would result in creep in compression at high loads is nullified by the creep in tension at light loads. That initial or preheat winding temperature which produces a minimum of permanent winding deformation for a given rotor during the expected operating cycle is the optimum preheat temperature for the given generator.

As the expansion of the rotor body is also a factor contributing to the total stress in the winding conductors, there is also an optimum preheat temperature for the rotor body, which, together with the optimum winding preheat temperature, assures lowest possible deformation. As the rotor winding temperature and the rotor body temperature differ by the temperature rise of the winding over the rotor body, it is to be expected that the two optimum temperatures, that of the rotor body and that of the rotor winding, will be found to differ. This situation points to preheating with current in the rotor winding as the best method and knowing the difference between the two optimum temperatures permits the determination of the required current.

In the determination of the optimum preheat temperatures two criteria exist, namely, minimum stress and minimum deformation. The two are not synonymous, since the latter is not only a function of stress but of the duration of stress as well. An example of a calculation for a preheat procedure is given in Appendix II of this paper in which equations having the criteria of minimum deformation, developed in Appendix III are used. The development of the equations of Appendix III are based upon an assumed behavior of copper under mechanical stress which is believed to be approximate to the actual behavior of copper in the temperature range and stress range as here applied, with sufficient accuracy for the purpose. Because of the necessary approximation it is desirable to test the result of calculation made with the method of Appendix III, by calculating expected extreme stresses as is done in the example of Appendix II. For most load cycles, the method of Appendix III will give tolerable values of stress. In the author's opinion tolerable values of stress for windings of soft annealed copper are in the order of those calculated in Appendix II, and should not exceed 5,000 pounds per square inch.

The refinement that can reasonably be incorporated in a preheating procedure is limited by practical considerations, of which a primary one is the accuracy with which rotor-winding and rotor-body temperatures can be determined. Experience shows that this accuracy is not likely to be greater than plus or minus five degrees centigrade. The assumptions in regard to the behavior of the conductor material, made in Appendix III must be viewed in this light, and when so viewed they become more acceptable.

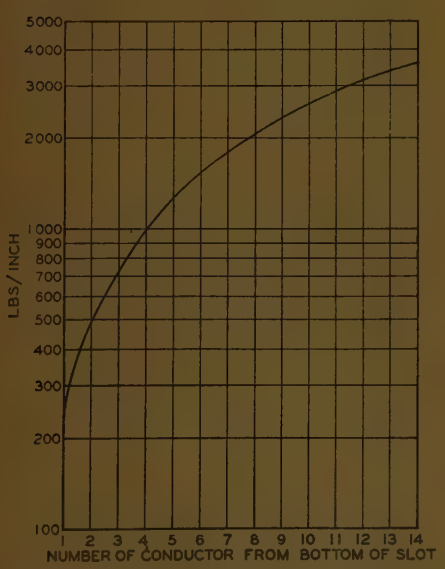


Figure 2. Centrifugal force on field-winding conductors at rated speed

Practical Aspects of Preheating

In the practical application of preheating of turbogenerator rotors, there are a number of factors, aside from those previously discussed, that must be considered if a workable procedure is to be obtained. Among these are:

(*a*). The arrangement of existing cooling system, that is, whether the generator is equipped with shaft driven blowers or with separately driven blowers, and whether the generator cooling system is a closed or open system.

(*b*). What means are available to furnish the required preheating current.

(*c*). There is the problem of co-ordinating the preheating procedure with the starting procedure of the turbine.

In regard to the existing cooling system, the preceding discussions show that it is desirable to obtain by means of preheating a certain definite rotor-winding temperature and also a temperature distribution throughout the winding as near as possible to that existing when the generator is in operation and carrying load. This requirement can be approached only if preheating is carried out with as near as possible normal air flow through the generator; however, it cannot be reached completely since preheating must be performed with the rotor revolving at low speed. On generators equipped with shaft-driven blowers, the requirements of nearly normal ventilation and low generator speed, of course, cannot be met simultaneously. In this case it may be necessary to use a procedure in which preheating is carried out in steps.* This is accomplished by starting the preheat at relatively high rotor speeds in order to provide the required ventilation and then at one or more intermediate temperatures reducing the speed, the steps being so timed as to prevent excessive winding stresses. With each step of speed reduction the accumulated stresses are released until, at the point where the specified preheating temperature is reached, a final slowing down releases all stresses before the generator is again accelerated and synchronized. On generators equipped with separate blowers, this difficulty is not likely to appear.

The nature of the cooling system is a factor in the preheating procedure because of the necessity to measure the rotor body temperature indirectly, that is, in terms of the winding temperature. This involves the thermal time constant of the rotor-winding-temperature rise over the rotor-body temperature. In view of the relative smallness of this time constant, it is convenient to eliminate it as a factor by holding the preheating current constant at that value which produces the expected rotor-winding-over-rotor-body temperature rise. However, with constant preheating current, the control of temperature lies in the adjustment of the cooling

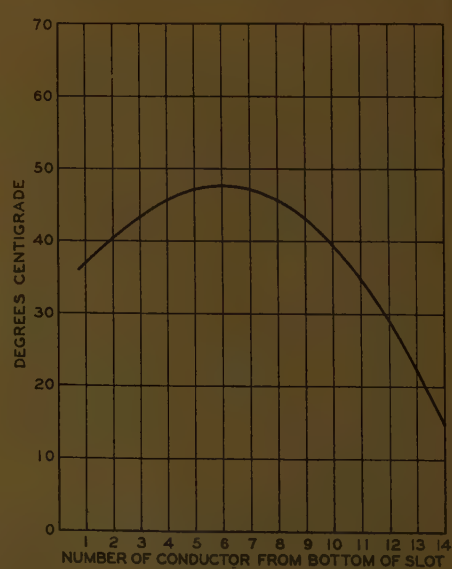


Figure 3. Temperature rise distribution in field coil

system. This is an easy matter on generators having a closed cooling system in which case the water flow through the coolers can be regulated. Where a generator operates on an open ventilating system the considerations of the preceding paragraphs in regard to temperature distribution within the rotor winding may have to be compromised because of the necessity of controlling the winding temperature by changes in turbine speed. Variations in the preheating current for the control of rotor-winding temperature should be resorted to only after all other means of controlling temperature are exhausted, since the attainment of the optimum rotor winding—rotor body temperature rise then becomes very difficult.

In regard to the availability of direct current for preheating, several additional factors enter into consideration. When a motor-driven or turbine-driven d-c variable-voltage generator of sufficient capacity is available, the problem is simple. Where the preheating current must be taken from a constant-voltage d-c source and where a field rheostat in the rotor circuit exists, the problem is equally simple. When neither exists, preheating can be carried on in steps at generator speeds sufficiently high to permit the use of the shaft exciter as power source. In the latter case, it may be found expedient to provide space heaters in the generator for the purpose of holding the generator at some elevated temperature from which preheating can be carried out in one step. Finally, the objective of preheating can be approached in some cases to a sufficient extent by the use of space heaters alone. The latter two methods may be satisfactorily applied in those cases where generators are taken off the line only infrequently so that higher stresses and larger values of deformation per cycle become acceptable.

The problem of co-ordinating a particular preheating procedure with the turbine starting operations contains so many factors in addition to those previously discussed, that an attempt to generalize is practically of no value.

Experience With Preheating

Preheating of rotor windings has been adopted for five generators on the system of the Consolidated Edison Company of New York, Inc., and the experience at the time of preparation of this paper extends over a period of approximately six months. The generators involved are turbogenerators of the following sizes: two generators, 200,000 kva, 60 cycles, 0.8 power factor, 1,800 rpm; one generator, 160,000 kva

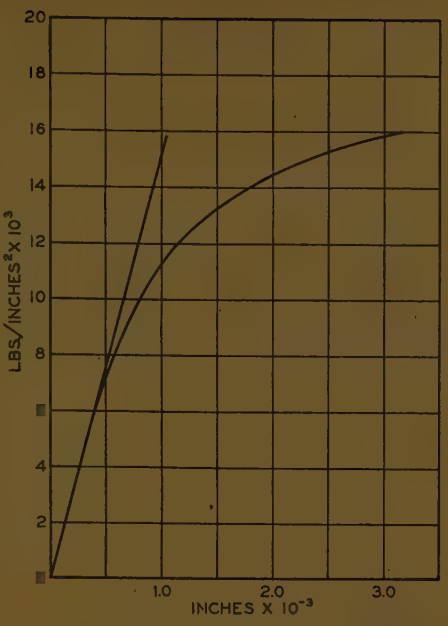


Figure 4. Stress–strain curve for soft annealed copper

25 cycles, 1.0 power factor, 1,500 rpm; and two generators, 60,000 kva, 25 cycles, 1.0 power factor, 1,500 rpm. Appendix I gives detailed data for the 160,000-kva, 25-cycle generator to which the example of Appendix II applies.

All these generators are equipped with external motor- or turbine-driven blowers, with closed cooling systems using air coolers, with shaft rotating gear and with field rheostats in the rotor circuit. Aside from differences in specified preheating currents and temperatures, the preheating procedures for all of these generators are substantially the same. Subsequent discussion will, therefore, be confined to the 160,000-kva 25-cycle unit.

The turbine starting operation and preheating procedure are carried out as follows:

1. Depending upon the duration of the preceding outage of the turbogenerator, that is, upon the initial temperature of the generator rotor, the time required for preheating is varied from one to two and one half hours. Generally, the "warming up" time of the turbine varies similarly, but it is possible to start preheating with the generator on the shaft rotating gear by means of which the shaft is rotated at approximately two revolutions per minute. Application of the preheating current is not permitted at standstill in order to avoid burning of the collector rings.

2. With the generator rotated by the turbine, not faster than 300 rpm, or with the generator on the rotating gear, the preheating current of 680 amperes (84 per cent of rated current) is applied to the rotor winding. Both generator cooling fans are started and provide practically normal air flow through the generator.

3. When the rotor winding approaches the specified preheat temperature, cooling water is admitted to control the temperature. The preheating current is held unchanged at a constant value. The preheat rotor-winding temperature is specified in relation to the prevailing cooling-water temperature as follows:

Cooling Water Temperature (Deg F)	Preheat Winding Temperature (Deg C)
Up to 45	65
46 to 60	72
61 and over	80

4. When the specified preheat temperature is reached and when the turbine is sufficiently warmed up, the generator is shut down to standstill and immediately restarted. This is done to permit complete relief of all stresses that may have accumulated in the winding. The preheating current still remains unchanged.

5. The turbine is now rapidly accelerated. As the speed increases above 1,000 rpm the operator reduces the preheating current just enough to prevent excessive generator voltage. When the generator reaches synchronizing speed the preheating current is removed, the shaft exciter is quickly switched to supply normal excitation, the generator is synchronized, and then loaded.

During the shutting-down period the cooling-water flow through the air coolers is reduced as the load is decreased to prevent excessive cooling, thus shortening the time required for subsequent preheating.

Calculations in Appendix II show that by using the above preheating procedure the stresses in the winding are 4,510 pounds per square inch in compression at maximum load and 3,790 pounds per square inch in tension at minimum load. Without preheating and starting at room temperature, the winding stresses would become 10,980 pounds per square inch in compression. It is expected that no

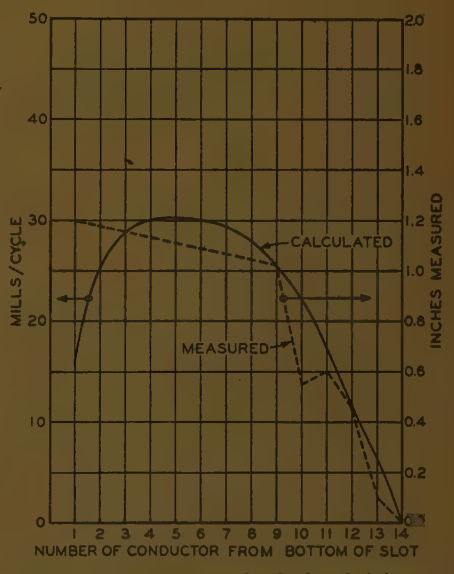


Figure 5. Measured and calculated deformation of a field coil

noticeable winding deformation will occur with the adopted preheating procedure.

The preceding example is an ideal one since facilities exist which permit the control of all of the diverse factors. Where such facilities do not exist, a great deal can still be gained by preheating in a less complete manner. So long as the rotor is brought to within its operating temperature range a material reduction in deformation can be expected.

Overloading of Generators

Frequently, particularly under the pressure of the present emergency, the problem arises whether a given generator can be overloaded with currents in excess of the rated field current and armature current. This problem becomes particularly pressing where a generator has a rated load temperature rise below that permissible by existing AIEE Standards. Very often this problem arises from the necessity to carry lower-power-factor loads and less often it arises from the availability of additional capacity in the turbine.

When temperature rise is less than the permissible at rated load it must not be assumed that the manufacturer had miscalculated in the design of the generator and in that way had really provided a generator larger than required. A safer assumption is that the manufacturer in designing the given generator was forced to compromise conflicting requirements with the result of having to design for a lower temperature rise in order to meet some other more pressing limitation. Among these limitations may well be that of field-winding deformation and if this be the major limitation, it may be possible to permit an appreciable increase in field current if preheating is adopted. Whether such overloading is at all permissible, and the extent to which it is permissible with preheating, is a subject to be thoroughly discussed with the manufacturer of the generator in question.

Conclusion

By means of preheating of rotor windings and rotor body of turbogenerators, rotor winding deformation can be minimized, with a consequent prolongation of the life of such windings. In some cases overloading of the generator can be made a safe procedure if preheating of the rotor windings and body is adopted. Practical procedures for preheating of rotor windings have been found to be easily coordinated with turbine-starting operations.

Appendix I

Data pertaining to the 160,000-kva 25-cycle 1,500-rpm turbogenerator.

Generator data:

Rating	160,000 kva
Power factor	1.0
Voltage	11.4 kv
Frequency	25 cycles
Maximum excitation voltage	250 volts
Rated field current	815 amperes

Generator-cooling-system data:

Type	Surface air coolers
Blowers	Separate motor and turbine driven
Quantity of cooling air	160,000 cubic feet per minute
Total heat loss, full load	2,245 kw
Total heat loss, no load	1,595 kw
Heat loss in rotor, full load	235 kw
Quantity of cooling water, maximum	1,000,000 pounds per hour
Maximum cooling water temperature	75 degrees Fahrenheit

Physical data:

Coefficient of linear expansion, copper—1.7×10^{-5} inch per inch per degree centigrade

Coefficient of linear expansion, steel—1.15×10^{-5} inch per inch per degree centigrade

Modulus of elasticity of copper—1.6×10^{7} pounds per square inch per inch

Generator operating data:

	Maximum Load	Minimum Load
Field current, amperes	875	460
Average rotor winding temperature, degrees centigrade	111.0	55.9
Average rotor body temperature, degrees centigrade	85.0	48.7
Airgap air average temperature, degrees centigrade	53.5	40.0
Air temperature into generator, degrees centigrade	40.0	30.4
Cooling water temperature, degrees Fahrenheit	75.0	75.0
Daily load cycle, hours	10*	12*

Appendix II

Example of calculation of the preheating procedure for a 160,000-kva 1.0-power-factor 25-cycle 1,500-rpm generator with data of Appendix I.

In the following all primed symbols refer to the minimum load condition and all double-primed symbols refer to the maximum load conditions and unprimed symbols refer to preheating conditions. All w subscripts refer to the rotor winding and all B subscripts refer to the rotor body.

From Appendix I:

$T_w' = 55.9$ degrees centigrade
$t_w'' = 111.0$ degrees centigrade
$T_B' = 48.7$ degrees centigrade
$T_B'' = 85.0$ degrees centigrade
$t' = 12$ hours
$t'' = 10$ hours

Using equations 11 and 12 of Appendix III

$$T_w = \frac{t'T_w' + t''T_w''}{t' + t''} = \frac{(12\times55.9)+(10\times111)}{12+10} = 81 \text{ degrees centigrade}$$

$$T_B = \frac{t'T_B' + t''T_B''}{t' + t''} = \frac{(12\times48.7)+(10\times85)}{12+10} = 65.2 \text{ degrees centigrade}$$

The preheating current is then

$$I = I''\sqrt{\frac{T_w - T_B}{T_w'' - T_B''}} = 875\sqrt{\frac{81-65.2}{111-85}} = 681 \text{ amperes}$$

The resulting stresses in the copper are found as follows:

$$s_w'' = \alpha_w(T_w'' - T_w) = 1.7\times10^{-5}\times(111.0-81) = 51.0\times10^{-5}$$
$$s_B'' = \alpha_B(T_B'' - T_B) = 1.15\times10^{-5}\times(85-65.2) = 22.8\times10^{-5}$$

then

$$f'' = M(s_w'' - s_B'') = 1.6\times10^{7}\times(51.0-22.8)\times10^{-5} = 4{,}510 \text{ pounds per square inch}$$

This stress is produced in compression at the highest operating temperature. At the minimum load conditions:

$$s_w' = \alpha_w(T_w - T_w') = 1.7\times10^{-5}\times(81-55.9) = 42.7\times10^{-5}$$
$$s_B' = \alpha_B(T_B - T_B') = 1.15\times10^{-5}\times(65.2-48.7) = 19.0\times10^{-5}$$
$$f' = M(s_w' - s_B') = 1.6\times10^{7}(42.7-19.0)\times10^{-5} = 3{,}790 \text{ pounds per square inch}$$

This stress is in tension.

For comparison of the efficacy of preheating, the stress produced during starting from an ambient of 40 degrees centigrade to maximum operating temperature is calculated:

$$s_w = \alpha_w(T_w'' - 40) = 1.7\times10^{-5}(111-40) = 120.4\times10^{-5}$$
$$s_B = \alpha_B(T_B'' - 40) = 1.15\times10^{-5}(85-40) = 51.8\times10^{-5}$$
$$f'' = M(s_w - s_B) = 1.6\times10^{7}(120.-51.8)\times10^{-5} = 10{,}980 \text{ pounds per square inch}$$

Nomenclature:

α_w = coefficient of expansion for the winding
α_B = coefficient of expansion for the rotor body
s = strain
f = stress
M = modulus of elasticity

* The remaining two hours are used in raising and lowering of load.

Appendix III

Nomenclature (for additional symbols see Appendix II):

T = temperature in degrees centigrade
a, k = constants
s, s_0 = plastic strains
u = creep rate
ϵ = base of the natural logarithm

Within the range of temperature and the range of stress occurring with preheating the creep rate is approximated by the following relation

$$u=\frac{ds}{dt}=af\phi(T) \tag{1}$$

where $\phi(T)$ is a function of the absolute temperature. For short-time creep rates very little is known about this function, although various attempts have been made[2] to approximate test results in long-time test. For this reason the function $\phi(T)$ is in the following analysis taken to be constant in the belief that this step will not greatly affect the result.

If an elementary length of conductor is stressed and then held so that its length remains invariant, the stress is relieved by creep according to the relation

$$-M\frac{ds}{dt}=\frac{df}{dt}=aMf \tag{2}$$

Setting

$$k=aM$$

for convenience,

$$-\frac{df}{dt}=kf \tag{3}$$

which has the solution

$$f=f_0\epsilon^{-kt} \tag{4}$$

where f_0 is the stress at the time $t=0$
Since

$$f_0=Ms_0 \tag{5}$$

and since

$$\begin{aligned} s_0''&=\alpha_w(T_w''-T_w)-\alpha_B(T_B''-T_B)\\ s_0'&=\alpha_w(T_w-T_w')-\alpha_B(T_B-T_B') \end{aligned} \tag{6}$$

The total strains accumulated during the times t'' and t' become

$$S_0''=[\alpha_w(T_w''-T_w)-\alpha_B(T_B''-T_B)]\times\int^{t''}\epsilon^{-kt}dt$$

$$S_0'=[\alpha_w(T_w-T_w')-\alpha_B(T_B-T_B')]\times\int^{t'}\epsilon^{-kt}dt \tag{7}$$

Integrating equations 7 and substituting the approximation

$$1-\epsilon^{-kt}=kt$$

$$\begin{aligned} S_0''&=[\alpha_w(T_w''-T_w)-\alpha_B(T_B''-T_B)]kt''\\ S_0'&=[\alpha_w(T_w-T_w')-\alpha_B(T_B-T_B')]kt' \end{aligned} \tag{8}$$

When the load cycle is complex equations 8 are used to determine T_w and T_B by trial and error. In most cases the load cycle is sufficiently approximated by calculation for maximum and minimum loads only. In the latter case equations 8 allow further analysis. Set

$$\begin{aligned} S_0''=S_0'=[\alpha_w(T_w''-T_w)-\alpha_B\times\\ (T_B''-T_B)]kt''=[\alpha_w(T_w-T_w')-\\ \alpha_B(T_B-T_B')]kt' \end{aligned} \tag{9}$$

Substituting in equation 9

$$r=\frac{\alpha_B}{\alpha_w}$$

and solving for T_w and t_B:

$$T_w-rT_B=\frac{t'T_w'+t''T_w''}{t'+t''}-r\frac{t'T_B'+t''T_B''}{t'+t''} \tag{10}$$

Since T_w and T_B in equation 10 are both unknown further, strict mathematical treatment is impossible. However, if

$$T_w=\frac{t'T_w'+t''T_w''}{t'+t''} \tag{11}$$

and if

$$T_B=\frac{t'T_B'+t''T_B''}{t'+t''} \tag{12}$$

are used, a satisfactory solution is obtained.

Appendix IV

When heating or cooling of a body, having thermal capacity, takes place in such a manner that the heat loss per unit time is directly proportional to temperature, the following relations apply.
Let

C = thermal capacitance in kilowatts per degree centigrade per hour
k = proportionality constant of heat dissipation in kilowatts per degree centigrade
H = heat loss in kilowatts
θ = temperature rise
T = time constant
t = time in hours

For heating

$$C\frac{d\theta}{dt}+k\theta=H \tag{13}$$

For cooling

$$C\frac{d\theta}{dt}+k\theta=0 \tag{14}$$

Equations 13 and 14 have the solutions

$$\theta=\theta_0\left(1-\epsilon^{-\frac{t}{T}}\right) \tag{15}$$

or

$$\theta=\theta_0\epsilon^{-\frac{t}{T}} \text{ respectively} \tag{16}$$

where

$$T=\frac{C}{k}$$

and where

θ_0 = ultimate temperature rise

For example, using values of Appendix I:
For the rotor windings

$$C_w=\frac{C_1W_1}{3{,}413}=\frac{0.167\times20{,}000}{3{,}413}=0.974 \text{ kilowatt per degree centigrade per hour}$$

For the rotor body

$$C_B=\frac{C_2W_2}{3{,}413}=\frac{0.212\times200{,}000}{3{,}413}=12.4 \text{ kilowatts per degree centigrade per hour}$$

where

C_1 = specific heat of copper in kilowatt-hours per pound per degree centigrade
C_2 = specific heat of steel in kilowatt-hours per pound per degree centigrade
W_1 = weight of windings in pounds
W_2 = weight of rotor body in pounds

For the rotor winding

$$k_w=\frac{H}{\theta w_0}=\frac{235}{26}=9.04 \text{ kilowatts per degree centigrade}$$

For the rotor body

$$k_B=\frac{H}{\theta_{B_0}}=\frac{235}{31.5}=7.46 \text{ kilowatts per degree centigrade}$$

Where θ_{w0} and θ_{B0} are the ultimate temperature rises of the rotor winding over the rotor body and of the rotor body over air-gap air, respectively.

$$T_w=\frac{C_w}{k_w}=\frac{0.974}{9.04}=0.1075 \text{ hour}$$

$$T_B=\frac{C_B}{k_B}=\frac{12.4}{7.46}=1.67 \text{ hours}$$

References

1. Deformation of Turbo-Alternator Rotor Windings, Due to Temperature Rise, G. A. Juhlien. *Journal* of the Institution of Electrical Engineers, London, England, volume 85, 1939, pages 544–52.

2. Creep and Relaxation of Oxygen-Free Copper, Evan A. Davies. *Journal of Applied Mechanics*, June 1943, pages A-101–5.

Blast-Tube Cooling for Aircraft Generators

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Synopsis: Established principles of hydraulics are used to develop and explain fundamentals of blast-tube cooling. It is shown that the pressure-volume curves of a blast tube can be determined by flight tests, and that the pressure-volume curve of a generator can be determined in the laboratory. From the two pressure-volume curves, the air-flow through the generator in flight can be predicted, without *an actual flight test on the generator*. This procedure would save many flight tests, if pressure-volume curves of various blast tubes were available. It is shown that the pressure-volume curve of a blast tube can be determined by open-end and closed-end readings on the blast tube, and the complete curve can be expressed in terms of two constants readily determined from these readings. Once values of the constants are established, air-flow calculations become as simple as the application of Ohm's law. The theory also covers generators with an internal fan. A plea is made for accurate blast-tube data and for better specifications on blast cooling. A suggested form of blast-tube specification is given.

BLAST cooling has been a major factor in obtaining the phenomenal outputs of modern aircraft generators from a small weight of material. This subject, in spite of its importance, seems to have been neglected in technical literature and, as a result, the fundamental principles do not seem to be at all well understood. Because of this lack, the subject has often appeared to be more involved than it actually is. It is generally understood that the amount of air flowing through a given generator depends upon the pressure differential available across the generator, and it is also well understood that the amount of pressure available from the blast tube depends upon the amount of air drawn by the generator. Because of this mutual interdependence, flight tests are today generally taken with a specific generator and blast tube, and it seems to be generally felt that it is necessary to test each different combination of generator and blast tube. Just as Ohm's law enormously simplified the calculation of electric circuits, so can the calculation of blast cooling be simplified by the use of similar concepts, which will be defined, explained, and used in this paper. This line of attack clarifies the fundamental principles involved, makes it necessary to run only one series of tests on any given blast tube, and one series of tests on any one given generator. It is not necessary to test each and every combination of generator and blast tube. This line of attack also makes it possible to write specifications which simultaneously accomplish a number of purposes:

(*a*). The airframe builder has a definite specification which can be used not only to design the blast-tube installation, but also to test it. He need not be concerned with different generator designs.

(*b*). The generator builder has a definite specification that permits him enough latitude to design a generator for maximum effective use of the available air.

(*c*). The specification clearly indicates how much pressure should be applied to the generator in laboratory tests to *simulate flight* conditions. (This point should be of especial interest to a procuring agency.)

The scope of this paper is limited to a study of air flow; it is not directly concerned with heat transfer and rating.

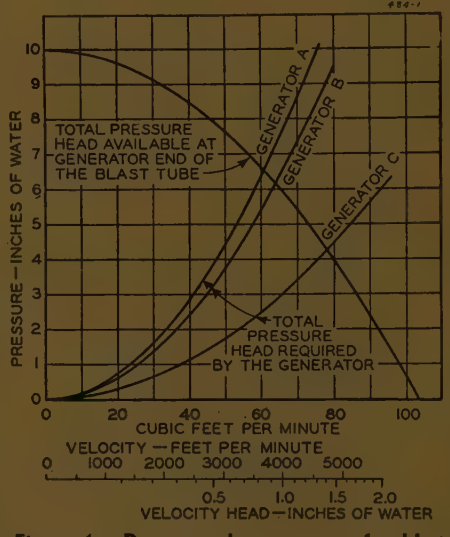


Figure 1. Pressure-volume curves of a blast tube and three representative aircraft generators

Pressure-Volume Curve of a Blast Tube

Pressure and volume flow in a blast tube can both be measured by means of the familiar pitot tube and either a manometer or an air-speed indicator. Three kinds of pressure can be read: static head, velocity head, and total head, which is the sum of the other two. In this paper, all pressures, except atmospheric, are measured in inches of water. Total pressure is a measure of the total energy in the air stream, and velocity head, for a fixed density, is a measure of the volume of air flowing.

For purposes of illustration, assume a specific blast-tube installation in a plane which is flying at a constant indicated air speed, and further assume that the position of oil-cooler flaps, or any other air dampers that might affect the flow of generator cooling air, remains fixed. Let the total pressure at the generator end be measured with the end capped so that no air can flow. Now, let the end be partially uncapped so that some air can flow, and let the volume and total pressure at the end of the blast tube be measured. This reading can be repeated for a number of different volumes until the tube is wide open, and the total pressure is then only the velocity head at exit. If now, the total head is plotted against the number of cubic feet of air drawn per minute, a curve will result, probably of the form shown in Figure 1, which is a parabola. This curve has been established without using a generator and may be called the "pressure-volume" curve of the blast tube. While it has not been established experimentally that the curve is a true parabola, the science of hydraulics indicates that it probably approximates one.

Pressure-Volume Curve of a Generator

Experiment and theory alike demonstrate that the pressure-volume curve of a generator is substantially a simple parabola. Three such curves, determined experimentally, are shown in Figure 1. The curve for each generator shows, for example, the pressure required to force any given volume of air through it. The three points where the three curves intersect the pressure-volume curve of the blast tube show the respective pressures and volumes that will be obtained by each generator. Thus the pressure and volume that each generator would receive from this blast tube can be determined without actually testing each generator with this blast tube.

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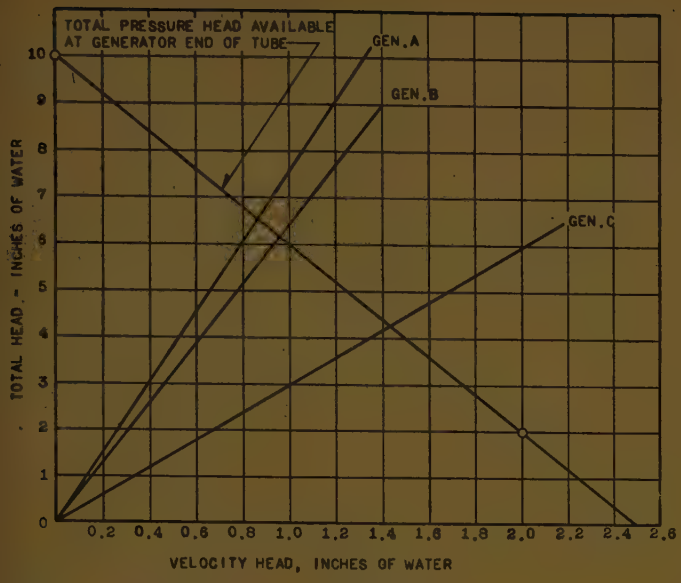


Figure 2. Pressure-volume curves of Figure 1 replotted as a function of velocity head

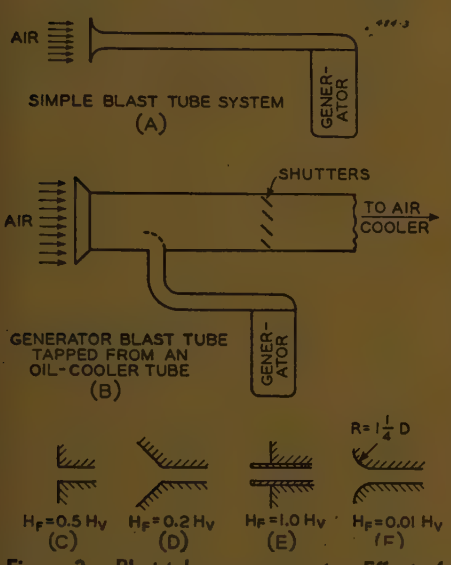


Figure 3. Blast-tube arrangements. Effect of shape of the entrance

The three generators for which the curves are shown are actual aircraft generators of different design which are in production use in aircraft today. None of them has an internal fan. The pressure-volume curve of the blast tube, necessary for purposes of illustration, was estimated

Table I. Tested Values of Air-Flow Resistance, R_g, for Three Different Designs of Aircraft Generators

(Based on a Two-Inch Blast Tube)

Conditions of Test	Generator A	Generator B	Generator C
Generator cold			
At standstill	6.51	5.90	2.80
At 2,500 rpm	6.82	5.71	2.87
At 3,500 rpm	6.94	6.04	2.90
At 4,000 rpm	7.15	6.19	2.91
After carrying 75 per cent load for one hour			
At 3,500 rpm	7.60	6.45	2.98

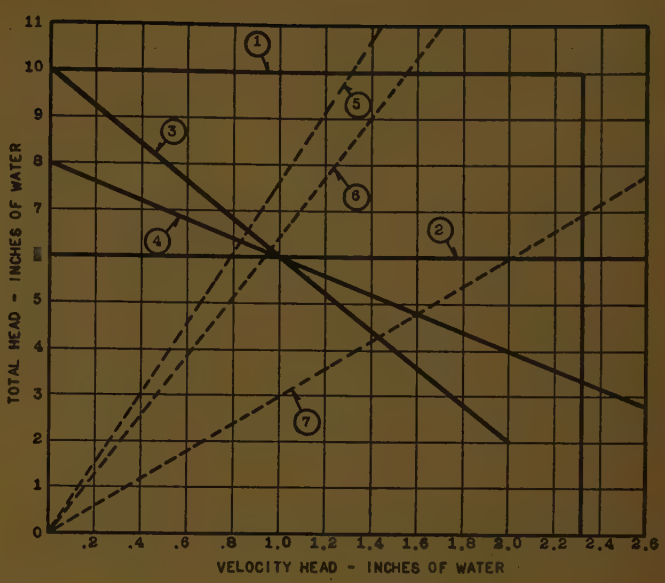


Figure 4 (right). A comparison of blast-tube specifications

1. PRESSURE-VOLUME CURVE PER SPEC. 95-32275 OF 3/3/42
2. PRESSURE-VOLUME CURVE PER SPEC. ANG-1-13, MAR. 43
3. PRESSURE-VOLUME CURVE PER SPEC. PROPOSED IN THIS PAPER
4. ALTERNATE PRESSURE-VOLUME CURVE PROPOSED
5. PRESSURE-VOLUME CURVE OF GENERATOR A
6. PRESSURE-VOLUME CURVE OF GENERATOR B
7. PRESSURE-VOLUME CURVE OF GENERATOR C

since no experimental curve was available. However, the pressures shown for the three generators approximate the pressures which have been observed in flight. These curves show how unreliable it is to impress the same pressures upon different generators for comparative tests. For example, suppose six-inch total head is impressed upon all three generators for test. The volumes of air that would be received with six-inch head applied are compared with the volumes received under flight conditions in the following table:

	Test	Flight
Generator *A*	58	61
Generator *B*	63	64
Generator *C*	93	78

Thus generator *C* would show 19 per cent higher temperature rise in flight than it would on laboratory test at six inches, although the tests on the other two generators would be approximately correct.

Alternative Method for Plotting Pressure-Volume Curves

In Figure 1, total pressure has been plotted as a function of volume. In Figure 2, the curves of Figure 1 are replotted with total pressure as a function of velocity head rather than volume. The pressure-volume curves become straight lines, which fact is an important advantage of this method of plotting. But much more important is the fact that, for all practical purposes, the curves of Figure 2 *are independent of air density, hence independent of altitude*, whereas the curves of Figure 1 are valid for only one air density. This statement is merely another way of stating the familiar principle of hydraulics that the lost head is proportional to the velocity head. Figure 1 could be valid for all altitudes, however, if the abscissae were understood to be "*indicated* cubic feet per minute," but the form of Figure 2 seems to be more convenient. Another advantage of using and plotting velocity head is that, when a pitot tube is used, *velocity head* is the quantity that is actually measured. Even if an air-speed indicator is used instead of a manometer, it is still velocity head which operates the instrument, though the scale is calibrated in miles per hour.

Calculation of Generator Pressures During Flight

In Figure 2, the pressure head across generator *A* and the velocity head (indicative of volume) of the air supplied was determined by finding the intersection of the pressure-volume curve of the blast tube with the pressure-volume curve of the generator, since the amount of head lost in the generator obviously has to be equal to that which is available from the blast tube. By using a concept of air-flow resistance, analogous to electrical resistance, this point of intersection can be calculated easily without plotting the curves of Figure 2. For any generator, the ratio

$$\frac{\text{head loss in generator}}{\text{velocity head at entrance}} = \frac{h_g}{h_v} = R_g \qquad (1)$$

is a constant, substantially independent of the pressure applied. This ratio R_g,

may be called the "air-flow resistance" of the generator. Similarly, the amount of head lost in a blast tube is proportional to the velocity head of the air flowing through it. Hence, the air-flow resistance of the blast tube may be defined as

$$\frac{\text{head lost in blast tube}}{\text{velocity head in blast tube}} = R_t \qquad (2)$$

The amount of pressure head available at the generator end of the blast tube must be equal to the head available at the scoop end, minus the head losses in the tube. The available head at the scoop end can be found by blocking the generator end of the blast tube and measuring the pressure h_c at the generator end, since, with no air flowing in the tube, there will be no head lost in the tube. Then, since the losses in the tube plus the losses in the generator must equal the available head, it follows from equations 1 and 2 that

$$h_c = h_v R_t + h_v R_g \qquad (3)$$

or

$$h_v = \frac{h_c}{R_t + R_g} \qquad (4)$$

The pressure across the generator is

$$h_g = h_v R_g = \frac{R_g}{R_t + R_g} \times h_c \qquad (5)$$

By way of illustration

For the blast tube of Figure 2

$h_c = 10.0$
$R_t = 4.0$

For generator B, $R_g = 6.45$
from equation 4

$$h_v = \frac{10}{4 + 6.45} = 0.958 \text{ inch}$$

and from equation 5

$$h_g = h_v R_g = 0.958 \times 6.45 = 6.16 \text{ inches}$$

which agrees with point of intersection shown in Figure 2.

Each blast-tube installation will have a different pressure-volume curve for each different indicated air speed of the plane. Likewise, if the generator blast tube is tapped off from a larger blast tube leading to an oil cooler, or intercooler, there will be a different pressure-volume curve for each position of the shutters in the main blast tube. Thus, the true performance of any given blast-tube installation can be represented by a family of pressure-volume curves, one for each significant flight condition. Each of these pressure-volume curves can be expressed, approximately at least, in terms of two significant constants, h_c and R_t. It is the author's opinion that, in any given installation, it is h_c which will show large variations with changes in flight conditions, and R_t will not vary greatly.

Flight studies of blast cooling should be made to determine the pressure-volume curves of the blast tube, either for the minimum condition, or for a number of conditions, as desired. For such flight tests, it is suggested that the generator be replaced by a device which incorporates a pitot tube and means for opening or closing the end of the tube in flight. If a series of flight tests are taken using a single generator, only a single point will be obtained on each pressure-volume curve. It is obvious that at least two points, preferably well separated, are necessary to establish a curve. For this reason, the author suggests taking open-end and closed-end readings, since these two readings establish the complete pressure-volume curve, or at least a fair approximation of it. More points could be taken, if greater accuracy is desired. However, regardless of the number of points taken, if the curve of pressure versus velocity head can be represented by a straight line, this line can be represented by two constants h_c and R_t, representing developed pressure and tube resistance, respectively. When h_c and R_t are known, and when the air-flow resistance R_g of a generator is known, the total pressure across it, and the velocity head can be quickly and easily computed from equations 4 and 5. From the velocity head, the volume and weight can be determined as indicated in the following paragraphs.

Determination of Air Velocity, Volume, and Weight From h_v

For National Advisory Committee for Aeronautics sea-level conditions of 15 degrees centigrade and 29.92 inches mercury, it can be shown that

$$v_m = 3{,}967 \sqrt{h_v} \qquad (6)$$

(This equation is based upon an air density of 0.07651 pound per cubic foot, water density = 62.37 pounds per cubic foot, and acceleration of gravity = g = 32.18. It further assumes that the air is incompressible.) At any altitude, where the density of the air is ρ pounds per cubic foot

$$v_m = \frac{1{,}097}{\sqrt{\rho}} \sqrt{h_v} \qquad (7)$$

Or, in terms of temperature T (degrees centigrade) and atmospheric pressure b in inches of mercury

$$v_m = \left(1{,}279 \sqrt{\frac{273 + T}{b}}\right) \sqrt{h_v} \qquad (8)$$

Or, if an air-speed indicator is used, the speed may be read in miles per hour, the pressure altitude and temperature noted, and the true air speed computed with a navigational flight computer.

For all velocity-head measurements, the pitot tube should be in the center of the pipe and should be preceded by a straight section of pipe at least 10 diameters in length. Average velocity in the blast tube may be taken as 80 per cent of the maximum velocity; and, if the sectional area of the tube is A square feet, the volume flow, at sea level, is, from equation 6

$$V = 0.80 \times v_m \times A = 3{,}967 \times 0.80 \times A \sqrt{h_v} = 3{,}174 A \sqrt{h_v} \qquad (9)$$

At altitude

$$V = 0.80 \times v_m \times A = \left(1{,}023 A \sqrt{\frac{273 + T}{b}}\right) \sqrt{h_v} \qquad (10)$$

The weight flow of air, in pounds per minute, is at sea-level

$$W = 242.8 A \sqrt{h_v} \text{ pounds per minute} \qquad (11)$$

at altitude

$$W = \left(753.4 A \sqrt{\frac{b}{273 + T}}\right) \sqrt{h_v} \qquad (12)$$

Thus, from the measured velocity head h_v, can be computed velocity, volume, and weight. If the generator builder is given values of h_c and R_t for the blast tube, he only needs to determine the air-flow resistance of a new generator in order to compute the velocity head, and from it the volume and weight flow for this new design without waiting for a flight test. Obviously, the same process could be applied to several alternative designs to determine which one would be best in a plane from the standpoint of effective cooling.

The Concept of Air-Flow Resistance

Air-flow resistance has been defined in this paper in a manner analogous to resistance in an electric circuit, so that it can generally be used in a manner analogous to the use of ohms in calculating d-c circuits. For example, since the generator and blast tube are in series, their "air-flow resistances" can be added directly. But a word of caution is necessary. The air-flow resistances of all the elements in any given air circuit must all be computed on the basis of the same diameter of tube at which the velocity head is measured. For example, the air-flow resistance of generator A is 7.60 based on a two-inch blast tube; if the diameter of the entrance were changed to three inches without changing

the generator otherwise, the resistance would then be $7.60\times(3/2)^2=17.1$.

Air-flow resistance has been used in a number of unpublished reports, defined in various ways. To avoid confusion, it is suggested that a name be applied to the unit of air-flow resistance as defined in this paper. Since this unit is so defined that a resistance of n units means a lost head of n velocity heads, the name "velhead" is suggested as appropriate. For example, generator A has an air-flow resistance of 7.60 velheads based on a two-inch tube. If no tube diameter is specified, it will be generally understood that the resistance value is based on the actual diameter of the air entrance of the generator.

Generators With Internal Fan

An internal fan will have the general effect of increasing the air flow. Let the pressure of the internal fan be denoted by h_{gf}. This pressure will be proportional to the square of the revolutions per minute and to the first power of the air density; it can be represented by the equation

$$h_{gf}=F_p\times\left(\frac{N}{1{,}000}\right)^2\times\frac{\rho}{\rho_0} \qquad (13)$$

or

$$h_{gf}=F_p\times\left(\frac{N}{1{,}000}\right)^2\times\frac{0.9626b}{273+T} \qquad (14)$$

where F_p is an empirical constant. Physically, it is the pressure developed by the fan at sea level at 1,000 revolutions per minute. This constant F_p might be measured by blocking either the inlet or the outlet and measuring the pressure at the blocked end of the generator; readings should be taken at different speeds.

When supplied by a blast tube, the velocity head of air flowing may be computed from equation 4 by adding the fan pressure to the numerator or

$$h_v=\frac{h_c+h_{gf}}{R_t+R_g} \qquad (15)$$

The volume can then be computed by using equation 10 and the weight of cooling air by using equation 12. The total pressure h_g across the generator is

$$h_g=R_gh_v-h_{gf}=\frac{R_gh_c-R_th_{gf}}{R_g+R_t} \qquad (16)$$

Using these equations, it can be shown that if to generator B a fan were added capable of delivering 1.5 inches pressure at sea level, the volume of air through the generator would be increased seven per cent, but the pressure across the generator would be reduced from 6.16 inches to 5.60 inches, a decrease of nine per cent. (Without numerical values of the two constants of the blast tube, h_c and R_t, these calculations could not have been made.) It is obvious that the addition of the fan should increase the volume of air and decrease the pressure at the inlet of the generator; but to determine quantitatively how great this effect is, it is necessary to know the two fundamental constants of the blast tube, h_c and R_t.

Suggested Tests for Blast Tubes

For a given installation, and for a given set of conditions, that is, for a given indicated air speed and position of the shutters in any air ducts associated with the generator blast tube, two readings are required: an open-end reading, and a closed-end reading. A pitot tube is installed in a straight section of tubing $1^{15}/_{16}$ inches in diameter approximately 20 inches long; the impact tube should be in the center of the pipe. The total-head connection of the pitot tube is to be connected to the pressure side of the manometer, or air-speed indicator; the static-head connection should preferably be capped, and the exhaust side of the manometer should be connected to the chamber into which the generator would discharge its exhaust air. Provision must be made so that the end of the blast tube can be either sealed tight or opened wide during flight, at will. For each significant set of flight conditions, two readings of the blast tube are to be taken, as follows:

(*a*). *Closed-end reading*, h_c=total pressure head above generator air discharge chamber, in inches of water.

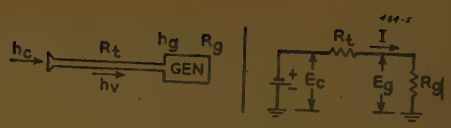
(*b*). *Open-end reading*, h_o=total pressure head above generator air discharge chamber, in inches of water. Record actual inside diameter of the pipe at the location of the pitot tube.

These tests are similar in principle to the classical open-circuit and short-circuit tests so frequently taken on various kinds of electrical machinery. The two blast-tube constants can be readily computed from these two readings as follows:

$$h_c=h_c \qquad (17)$$

$$R_t=\frac{h_c-h_o}{h_o} \qquad (18)$$

h_o is a velocity head since the static head when the tube is discharging freely, is zero. The altitude at which the plane is flying for these readings is not particularly important, since the density of the air should not affect the test results appreciably. If the velocity of air in the blast tube is desired for some reason or other, it may be determined from equation 8, the volume from equation 10, or the weight from equation 12.

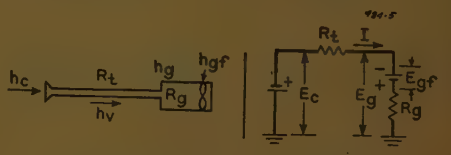


No Fan in the Generator

Closed-end head $=h_c$ | E_c =open-circuit volts
Blast-tube resistance $=R_t$ | R_t =line resistance
Velocity head of flow $=h_v$ | I =current
Generator resistance $=R_g$ | R_g =load resistance

$$h_v=\frac{h_c}{R_t+R_g} \qquad I=\frac{E_c}{R_t+R_g}$$

$$h_g=h_vR_g=\frac{R_gh_c}{R_t+R_g} \qquad E_g=IR_g=\frac{R_gE_c}{R_t+R_g}$$



With Internal Fan in the Generator

$$h_v=\frac{h_c+h_{gf}}{R_t+R_g} \qquad I=\frac{E_c+E_{of}}{R_t+R_g}$$

$$h_g=h_vR_g-h_{gf} \qquad E_g=IR_g-E_{gf}$$

$$=\frac{R_gh_c-R_th_{gf}}{R_t+R_g} \qquad =\frac{R_gE_c-R_tE_{gf}}{R_g+R_t}$$

Note: If h_g is −, generator fan helps overcome R_t as well as R_g, and more air would flow through generator if blast tube were removed from generator.

Note: If E_g is −, E_{gf} helps overcome R_t as well as R_g, and more current would flow if terminals E_g were short-circuited.

Figure 5. Electric circuit analogue

Tests to Determine Air-Flow Resistance of Generators

Generator air-flow resistance can be measured in the laboratory by applying a total head, h_g, to the generator air inlet, and measuring the velocity head, h_{vg}, in the blast tube. The air flow resistance of the generator is:

$$R_g=h_g/h_{vg} \qquad (19)$$

Values of R_g were determined experimentally by G. G. Setterlund, who obtained the results shown in Table I.

These results indicate that there is some variation in air-flow resistance with temperature and with speed and, although these changes are comparatively small, it is recommended that the air-flow resistance be measured at conditions ap-

Table II. Air Pressures on Generators During Flight

(Reference, Figure 4)

	Generator A	Generator B	Generator C
According to curve 1.....	10	10	6.9
According to curve 2.....	6	6	6
According to curve 3.....	6.55	6.17	4.28
According to curve 4.....	6.33	6.10	4.79

proximating full-load temperature and normal operating speed.

If the generator has an internal fan, the pressure h_{gf} from it at normal operating speed should first be measured by blocking the inlet or exit. Then, with the machine operating at the same speed and at approximately full-load temperature, impress a head h_g and measure the velocity head h_{vg} of the inlet air. The air-flow resistance will be:

$$R_g = \frac{h_g + h_{gf}}{h_{vg}} \qquad (20)$$

This value of Rg will include the resistance of the internal fan, as it should.

Factors Affecting h_c

If the air scoop were out on a wing where it was unaffected by the slip stream, as pitot tubes for air-speed indicators are, and if the air from the blast tube were discharged to atmospheric pressure, h_c would be simply the airplane pitot pressure, which is the same for all planes. It would be approximately

$$h_c = \left(\frac{\text{indicated miles per hour}}{45.1}\right)^2 \qquad (21)$$

(For high air speeds, h_c would be slightly more than indicated by the above.) If the scoop is behind the propeller, h_c could be considerably above airplane pitot pressure. However, if the back pressure in the air discharge chamber is substantially above airplane static pressure, as tests indicate it often is, the net pressure h_c may be either greater or less than airplane pitot pressure, that is, greater or less than would be indicated by equation 21. An ideal arrangement would be a short tube with a minimum number of bends, with a curved-funnel scoop directly in the propeller blast, as shown in Figure 3A.

Quite different is the arrangement shown in Figure 3B where the generator air supply line is merely tapped into a larger blast tube supplying the oil cooler, intercooler, or both. In this case, h_c will depend not only upon the speed of the plane, but also upon the position of the shutters; h_c will be greater when they are closed than when they are open. The quantity h_c can be increased some by use of a scoop at the entrance to the generator tube to take advantage of the velocity head of the air in the main duct. For this arrangement, the worst condition is with shutters wide open and plane flying at minimum speed.

If a flexible air tube is used, there may be considerable air leakage, which will reduce appreciably the observed values of h_c and h_o.

Factors Affecting R_t

The air-flow resistance of the blast tube, R_t takes into account the head losses in the blast tube including entrance losses. Numerically, it is equal to the number of velocity heads lost in the tube. The losses consist principally of: entrance losses, fluid friction at the sides of the tubes, losses in elbows or bends in the tube, and losses from changes in cross-sectional area of the tube, if any. The loss at the entrance is affected by the shape of the entrance. Figures 3C to 3F show different shapes of entrances and the probable head loss due to each. (These are the head losses usually obtained when a constant static head is maintained at the entrance; whether or not the same losses are obtained when the tube itself is moved against the air is problematical, but it seems reasonable to suppose that these figures do afford a comparison of the relative effects of different shapes of entrance.)

In general, in any fixed plane installation, the tube resistance R_t probably does not change greatly with changes in plane speed or changes in shutter position. However, there may be an exception in the case of an installation such as Figure 3B if the amount of air drawn by the generator is an appreciable part of the total air, because the air drawn by the generator will increase the drop between the entrance of the main scoop and the entrance to the generator tube. Thus, with shutters closed, both h_c and R_t may be higher than when the shutters are open.

Blast-Tube Specifications

Pressure-volume curves corresponding to four different blast-tube specifications are given in Figure 4, together with pressure-volume curves of three representative aircraft generators. Curve 1 illustrates specification 95-32275, amendment 2, dated March 3, 1942, which specifies ten inches total pressure, but not over 100 cubic feet per minute; the vertical line is drawn at a velocity head corresponding to 100 cubic feet per minute. Curve 2 illustrates specification ANG-1-13 which specifies six inches of water, with no limit on volume. No blast tube could have a pressure-volume curve corresponding to either curve. Curve 3 is the pressure-volume curve repeated from Figures 1 and 2 of this paper. Curve 4 shows a different idea as to what the true pressure-volume curve may be. According to these specifications, the pressures received by the three generators in flight will be indicated by the points of intersection (Table II). Then, according to these specifications, the pressures impressed on the generator for chamber tests should be according to the table.

Taking curve 3 as a reasonable representation, it is suggested that the specification for a two-inch diameter blast tube might be worded somewhat as follows:

The blast tube shall be designed to deliver, for any condition of flight, a total pressure head at the end of the blast tube of at least ten inches of water when the end is closed and at least two inches of water when the end is not restricted. This pressure head shall be the total head at the end of the tube above the ambient air pressure in the generator compartment. When the open-end reading is being taken, the blast tube shall be discharging its air into the generator compartment and the pitot tube shall be in the center of a two-inch-diameter straight pipe preceded by a straight length of ten or more diameters.

Such a specification would be more informative and helpful to a generator designer, than present specifications, because he could then determine in advance what air pressures and volumes his generator would receive in flight. Moreover such a specification would be of advantage to a procuring agency when any new generator was received because it could run a simple air-flow test on the generator in the laboratory, and by very simple calculations determine the pressure and volume the generator would receive in flight, *without actually making a flight test.* This same pressure could be applied in laboratory chamber tests. However, it is to be hoped that pressure-volume curves for representative installations will be obtained by actual flight tests so that specifications can be based on actual conditions.

It should be emphasized here, that there is no magic figure for the air requirement for any generator; in general the more air it gets, the better will be the performance, which may show up in the form of more load capacity, particularly at a high altitude, lower temperature rise, greater reliability, or longer life.

Conclusions and Remarks

Fundamental theories of blast-tube cooling have been developed and their use explained. The problem of blast cooling is broken down into two parts: a study of the blast-tube installation itself, and an independent study of the generator itself. It is shown that the performance of the blast tube can be expressed in terms of two factors: internal pressure h_c (that is, pressure available with no flow) and air-flow resistance R_t, a quantity analogous to ohms in a d-c circuit. Values of these two constants, which vary somewhat, de-

pending upon the installation, upon plane speed and other factors, are all that is required to predict the air flow through a generator which has been tested independently of the blast tube. Tests on the generator alone can be made to yield, in the case of a generator with no internal fan, a single constant, the air-flow resistance R_g. If the generator has an internal fan, more tests are required on the generator to determine the characteristics of this fan, but no more tests are required on the blast-tube installation. In general, the method of attack is: first, the air-flow resistances of the generator and blast tube are obtained; second, the velocity head of air flow is computed from the closed-end pressure and resistances; third, the velocity, volume, and weight are then computed from the velocity head.

A more rational and illuminating method of specifying the minimum characteristics of a blast tube is proposed. The new method of specification would provide the airframe builder with a clear-cut specification which could be used for design and checking of the blast tube, it would provide the generator builder with a rational basis for designing his cooling system in the generator for maximum effectiveness, and, further, would provide a simple method for determining what pressure to apply to any given generator for sea-level tests, either at atmospheric pressure or in an altitude chamber.

The equations developed for blast-tube cooling are compared with similar equations for electric circuits in Figure 5.

Symbols

A = cross-sectional area of blast tube at pitot-tube location, square feet.

b = atmospheric pressure of air, absolute, in inches of mercury.

F_p = pressure coefficient of the internal fan of a generator. See equation 13.

H_f = lost head (Figure 3).

h_g = total head across the generator, inches of water.

h_{gf} = pressure developed by an internal fan, inches of water. See equation 13.

h_c = total pressure head at generator end of blast tube above generator ambient, end of tube closed.

h_o = total pressure head at generator end of blast tube above generator ambient, end of tube wide open and unobstructed.

h_v = velocity head, in inches of water.

h_{vg} = velocity head in blast tube when supplying air to a generator.

N = revolutions per minute of the generator.

T = temperature of air entering blast tube, in degrees centigrade.

R_g = air-flow resistance of a generator. Defined in equations 1, 19, and 20.

R_t = air-flow resistance of a blast tube. Defined in equation 2.

v_m = maximum velocity of air (at center of blast tube) in feet per minute.

V = volume of air flowing, in cubic feet per minute.

W = weight of air flowing, in pounds per minute.

ρ = density of air, in pounds per cubic foot.

ρ_0 = density of air at NACA sea-level condition (15 degrees centigrade, 29.92 inches of mercury) = 0.07651 pound per cubic foot.

Aids to Quality Control in the Manufacture of Aircraft Generators

F. M. POTTER
NONMEMBER AIEE

AIRCRAFT generators are designed for high output, low weight, small size, and wide speed range. These limitations require close designs which do not permit normal allowances for material and manufacturing variations. Small variations in the material and manufacturing processes result in wider variations in output characteristics than in conventional machines.

It is the purpose of this paper to discuss certain of these variations; their effect on the output characteristics of the machine, and to illustrate means of checking and controlling these factors.

Factors Affecting Output Characteristics

1. Shunt-field ampere turns available, compared to those required by the magnetic circuit, determine the base or underspeed characteristic.

2. Type of insulation, heat transfer coefficients, shape and size of cooling ducts, and the amount of cooling air taken through the generator determine the losses which can be dissipated; and in turn, the output rating.

3. Commutating ability, generator stability, temperature rise, and mechanical construction determine the top operating speeds.

Variations Affecting Required Shunt-Field Ampere Turns

For a required total flux, shunt field ampere turns are affected by the following:

1. Variations in air-gap length.

2. Variations in iron portion of the magnetic circuit.

(*a*). Magnet frame—permeability.

(*b*). Main poles—stacking factor and permeability.

(*c*). Armature punchings—stacking factor and permeability.

3. Variations in compensation of armature reaction.

Variations Affecting Temperature Rise

The temperature rise of an aircraft generator depends mainly upon:

1. The total losses.

2. The amount of cooling air.

3. The effectiveness of this cooling air; that is, where it goes and the area of winding surface it wipes.

Variations in Final Machines—Testing Technique

The assembled generator combines all the variations of its individual parts and then is subject to the variations of testing technique, which, when broken down, consist of: brush fit, brush setting, instrumentation, and the human element.

Variations in Air Gap Length

In the General Electric type *P*-1 aircraft generator rated 30 volts, 200 amperes, 2,200/4,500 rpm, forced-cooled, six-inches-water air pressure, approximately 70 per cent of the shunt field ampere turns are consumed in the nominal air gap. This part of the magnetic circuit, therefore, warrants all possible control of its variations.

Drawing tolerances, consistent with manufacturing capabilities and engineering requirements, permit a normal variation in the air gap. In addition to

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Figure 1. Equipment for determination of gaps between magnet frame and main poles

the expected variations are the unexpected and undesired variations due to faulty manufacture and assembly of parts. It is these undesired variations which are eliminated by means of a special device termed a "gap checker." See Figure 1. In reality this device searches for and locates gaps between the main poles and the magnet frame.

The "gap checker" works on the bridge-circuit principle, using either flashing direct current or low-frequency alternating current as a source of power for the excitation coil. Intimate contact with the poles being checked is obtained by making the radius of the leg faces equal to that of the pole bore, and then supporting the entire weight of the field assembly from the contact faces.

If the total flux divides equally between the two parallel paths, the voltage induced in each of the bridge coils will be the same, and since they are connected opposing, no voltage will be read on the instrument. This indicates that both poles being checked are identical with regard to contact with the frame.

The air gap on the side opposite the poles being checked is large so that variations on this side of the field assembly will have little or no effect on the flux division in the legs of the device.

Experience in using this "gap checker" shows that there is usually a small voltage showing on the instrument if the poles have a normal fit against the frame. The center-zero instrument is therefore blocked off in red except for a small portion around the zero. If the needle goes into the red portion, one of the poles being checked has a gap in back of it. To determine which of the poles has the gap, the frame is rotated one pole pitch and the test repeated. If the high reading still exists, the pole of the original pair still being tested is the one at fault. If the high reading no longer exists when the frame is rotated one pole pitch, then the faulty pole is that one of the original pair which is not in the second pair.

Strictly, this device is a comparator, comparing the magnetic circuit of adjacent main poles. The use of such a device is based on manufacturing experience which shows that in those field assemblies faulted for this cause, there is usually only one faulty pole and seldom if ever more than two.



Figure 2. Test equipment for determination of magnet-frame permeability

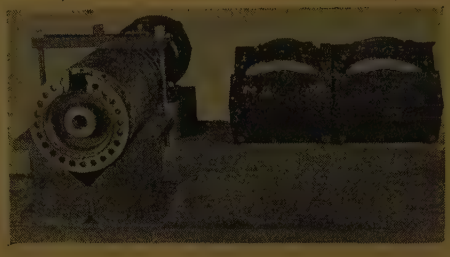


Figure 3. Equipment for spot-checking main-pole stacking factor and permeability



Figure 4. Armature-punching - permeability meter

Variations in the Iron Portion of the Magnetic Circuit

Magnet Frame

During these wartime conditions, it has sometimes been found necessary to accept inferior and variable magnet-frame materials in order to keep the production lines going. Laboratory tests of several samples have shown the frame material to be extremely variable in its magnetic properties, some of it good, some of it definitely bad, and some of marginal properties. A laboratory test of each frame is obviously a production impossibility.

The "frame tester" shown in Figure 2 was therefore designed for making quick production line checks of the magnetic properties of the frame material.

The "frame tester" consists of a standard *P*-1 generator armature supported in a structure which includes one fixed main pole and one movable main pole 180 degrees apart. Field coils separately excited supply the required ampere turns. The armature is directly connected to and driven at constant speed by a one-quarter-horsepower induction motor. The voltage generated in the armature is picked up by a pair of brushes and read with a standard d-c voltmeter.

To check a frame, it is necessary to take only one reading. The frame to be tested is slid into position. Then, with the armature running, the field circuit is closed and a specified field current set. The armature voltage is then read. This reading is indicative of the magnet-frame permeability.

The value of this "frame tester" is that poor material is rejected at the earliest possible stage in the manufacturing cycle; that is, immediately after the boring and turning operation.



Figure 5. Equipment for preliminary test of armatures



Figure 6. Current-balance test equipment

Main Poles

Rigid adherence to specifications by the vendors of magnetic sheet steel, careful laboratory checks of all shipments received, and accurate control of annealing processes have resulted in little or no trouble magnetically in main poles.

However, as a guard against any trouble which might arise due to substandard poles, and thus interfere with production, the equipment shown in Figure 3 was made for making spot checks on various lots of poles.

The "pole tester" is similar in principle to the "frame tester" in that a voltage generated in a standard armature at a definite value of field excitation is the criterion for a passable pole. In this device the pole to be tested is slid through the opening in the mounting flange and screwed into position opposite a fixed pole around which the exciting coil is mounted. Poles to be so tested are ground to a definite radial length so that air-gap variations will not enter into the result.



Figure 7. Shunt-field-coil mold and short-circuit tester

Armatures and Armature Punchings

In addition to standard laboratory tests on magnetic sheet steel for armature punchings, it has been found desirable to make spot checks of armature punchings after the annealing process. The annealing process is of course an important operation and if done improperly, the punchings will not equal the required magnetic values.

An "armature-punching tester" is shown in Figure 4. A single armature punching makes up the magnetic circuit. Low-voltage 60-cycle alternating current obtained by means of a Variac transformer and a current transformer is applied to the one-turn winding through and around the core section of the armature punching. For a specified exciting current, the voltage induced in a five-turn secondary, which is proportional to the flux carrying properties of the punching, is read. Comparison of this reading to a minimum standard for the particular thickness of the punching indicates whether the punching is up to specified requirements.

Figure 5 shows equipment known as an "armature tester." This equipment consists of a pair of short poles mounted on a bed plate together with a pair of brush holders and brushes, a fixed ball-bearing holder on the drive end, and an adjustable ball-bearing holder on the opposite end which permits the device to be used for testing six-pole armatures of various heights of iron.

To test an armature, lapped ball bearings are placed on the armature shaft, the armature put into position, and the drive shaft brought into engagement with the inner splines of the armature shaft. The motor is then started up, a definite exciting current applied to the field coils, and the voltage generated by the armature read. If the voltage reading is below a specified value, the armature is rejected.



Figure 8. Shunt-field-coil turn counter



(A)

Figures 9A and 9B. Shunt and commutating coils, before and after molding

By means of this device, the following faults are determined:

1. Poor magnetic properties of the stacked core.
2. Wrong connections of the armature winding.
3. Short circuits in the armature winding.

Variations in Compensation of Armature Reaction and Interpole Strength

The armature reaction per pole at full rated current of the General Electric type *P*-1 aircraft generator is 960 ampere turns as compared to a maximum of 480 ampere turns in the shunt field. Pole face or compensating windings are therefore used to neutralize partially the effect of the armature reaction. Interpoles and interpole windings are also used to obtain sparkless or "black" commutation throughout the speed and load range. These windings are connected in three parallel circuits, each circuit consisting of two commutating and two compensating coils in series.

To obtain the full benefit of these compensating- and commutating-pole windings, it is necessary that the three parallel circuits balance and that the load current divide equally between them.

Due to difficulty in accurately measuring these low-resistance circuits, a check of actual current balance is taken by means of the special setup shown in Figure 6. A small arc welder supplies approximately 75 amperes and the division of this current between the three field circuits is simultaneously read on the three ammeters. Limits of unbalance have been set beyond which fields are rejected. This test has proved its worth to the extent that two such stations are used, one before the field is impregnated,



(B)

and the other just before the field is assembled into the final machine.

At the first station, that is, before the impregnation treatment, immediately after the current readings balance are taken and with current still flowing, a polarity check of the windings is made by use of a compass.

Variations Affecting Temperature Rise

In a forced-cooled aircraft generator practically all of the losses are carried away by the cooling air. It is, therefore, of paramount importance to obtain the maximum possible amount of cooling air through the right passages and to have this air wipe the maximum area of winding surface.

The General Electric type *P*-1 generator has been designed with several ducts through the field and with the maximum area of winding surface exposed to the cooling air. These ducts result naturally when the shunt and commutating coils occupy their allotted space. In order to maintain uniform air ducts through the fields, it is necessary to shape the shunt coils and to compress and mold



Figure 10. Field air-flow measuring instrument

them to a definite size. To do this, these coils after being wound on standard winding machines are impregnated in a molding varnish, pressed and baked in heated molds as shown in Figure 7.

Coils, when removed from the press and while still hot, are checked for short circuits on the "short-circuit tester" shown also in Figure 7. This device is a sensitive bridge circuit operating on 60 cycle power. A short circuit in the coil unbalances the magnetic field causing the instrument needle to deflect.

Figure 8 indicates still another checking station, this time a "turn counter." Shunt field coils which have passed the "short-circuit tester" are checked for proper number of turns by balancing the voltage generated in the coil undergoing test to that of a standard coil. The instrument is marked off to permit coils to pass with plus or minus one turn from the standard.

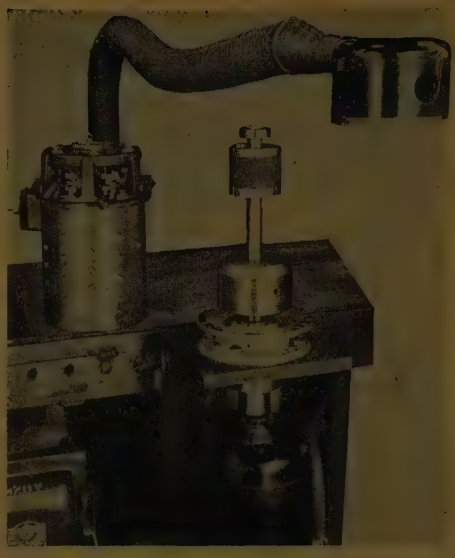


Figure 11. Brush-seating machine

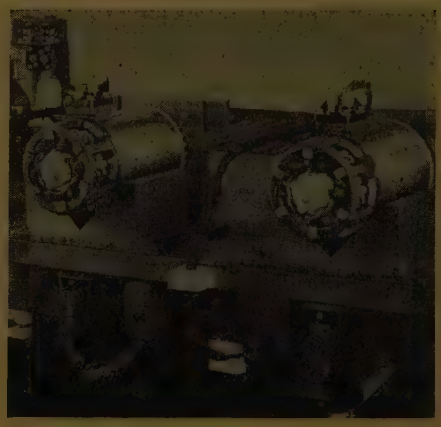


Figure 12. Motor run-in stands

Commutating coils are wound in what is termed a "back turn winding" directly onto the insulated pole. These wound poles are then placed between hot pressing plates, pressed to definite dimensions, and baked to set up the special thermo-setting varnish on the glass covered wire.

Figures 9*A* and 9*B* show shunt and commutating coils before and after molding. Note also the portion of the commutating pole projecting beyond the winding. This is the frame side of the pole, and when the pole is assembled into the magnet frame, an air duct is obtained between the frame and the under side of the commutating pole winding. Such molded shunt and commutating coils also provide an air duct between the shunt and commutating coils.

Manufacturing variations may result in too little air flow through the field. A "field-air-flow measuring instrument" as shown in Figure 10 was therefore designed in order to check the air passages through the field. The instrument is located in the production line at an early stage in the manufacturing cycle, where a field can be easily repaired if it has been rejected for restricted air passages.

The "air-flow instrument" consists of a blower which forces the air through an air-flow-measuring box shown under the table. A short pipe connects the exhaust of the box to the flange on which the field assembly is placed. A dummy armature forces the air to go through the passages in the field.

To make use of this instrument, a field assembly to be checked is placed over the dummy armature and rested upon the flange. A static pressure of 2.5 inches of water, measured in the pipe just below the flange, is set, and the differential

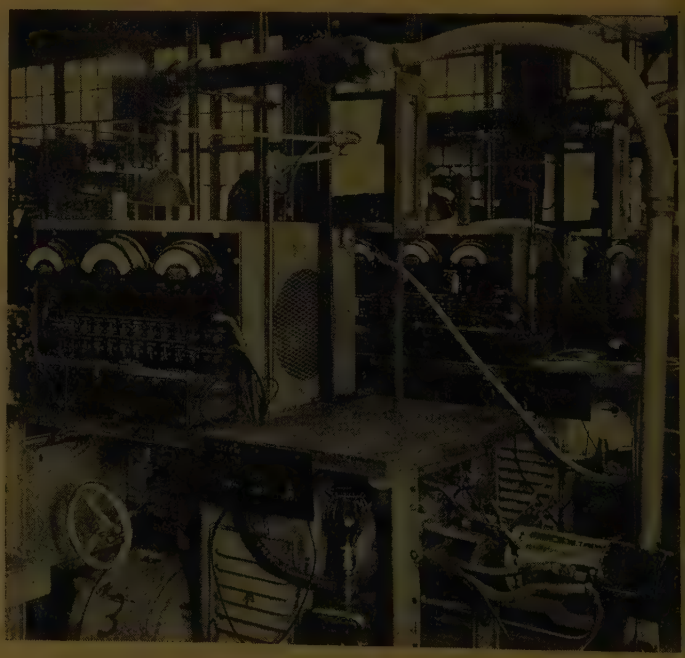


Figure 13. Generator test stands

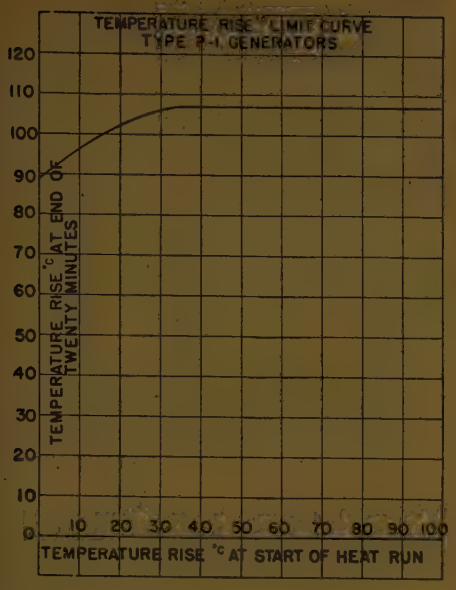


Figure 14. Shunt-field temperature-rise curve

pressure across the orifice in the air-flow box is read. This reading determines the air flow through the field.

Tests have shown that by means of this device, the temperature rise of any field can be quite accurately predicted.

Variations in the Assembled Generator

The assembled generator combines all the variations and tolerances of its individual parts. It is only by diligently and resourcefully controlling the variations of these component parts that consistent generators are manufactured.

The assembly and final test of the completed generators bring out the inconsistencies and reject those generators which do not meet specification requirements. Here again, however, variations arise in the tests themselves. Testing variations can be controlled by:

1. Proper supervision and instruction of the testers.
2. Proper instrumentation.
3. Proper brush fit and correct brush setting.

Figure 11 shows a "brush-seating machine" which is a very definite aid in obtaining excellent brush fits in a short time. Brushes as received from the vendor are ground to approximately the correct radius. The brush rigging with the brushes in place is assembled with the field and endshield, all screws are tightened, and the brush rigging set in approximately the correct position. The "brush-seating machine" is then used to obtain a nearly perfect brush fit. The combined assembly; field, brush rigging,



Figure 15. Vibration test stand

and endshield is placed over the arbor. The brushes are placed in their respective holders onto the grinding arbor, and held in their normal position by the brush springs. The exhaust hood is dropped over the endshield, the motor started, and allowed to drive the grinding arbor in the normal direction of rotation for approximately 15 seconds. At the end of this time, the brushes are properly fitted and very nearly polished.

The abrasive for this fixture is crocus cloth. The diameter over the crocus cloth is equal to the nominal diameter of the commutators. Very little of the brush is removed during this operation, the maximum amount ever experienced being not over 0.010 inch.

Figure 12 shows a pair of motor run-in stands where the generators are run as motors for approximately five minutes to perfect the brush fit. Occasionally a machine requires the application of a soft "brush-seater" to obtain the required brush fit. Here, also, electrical or so-called "inductive neutral" is located, and the brush rigging set in this position. The generator is then sent on to the final test.

Final Test

Figure 13 shows a group of test stands where the generator receives its final tests. These final tests consist of the following:

1. Setting of final brush position.
2. Base-speed heat run.
3. Regulation at base and top speeds.
4. Commutation check at top speed, full load.
5. Overspeed test.
6. Repeat of number 4.
7. Vibration reading—generator run as a motor.
8. High potential test.

Because of certain unique features, two of the tests listed will be discussed briefly.

Base-Speed Heat Run

All generators are subjected to a base-speed full-load heat run, the duration of which is either 20 minutes or until temperatures and field current readings have stabilized. Test limits for 20 minute runs have been established whereby a generator having a temperature rise of X degrees centigrade at the start of the heat run must not exceed Y degrees centigrade temperature rise at the end of 20 minutes, or more than a certain value of field current; otherwise, the heat run must be continued to the stabilization point. These limits of temperature rise for 20 minute heat runs are shown in the form of a curve in Figure 14. This curve is the result of a large number of tests and assures that any generator meeting the 20 minute heat run limits will meet the limits for ultimate heat runs.

To obtain consistent and accurate heat run data, each test stand is equipped with identical blast tubes and covers as shown in Figure 13. All voltmeters, ammeters, air-pressure gauges, and speed tachometers are carefully checked, and recalibrated, if necessary, at least once a week.

Vibration

Vibration or balance readings are taken on each generator after the completion of tests. With the generator mounted in the "vibration stand" as shown in Figure 15 and running as a motor, actual vibration readings are taken at each end of the generator by means of an electric vibrometer.

The vibration stand itself consists of a somewhat flexible steel table on which is mounted a heavy steel plate insulated from the table top by means of sponge rubber pads. The generator to be tested is mounted on a felt pad in a "V" shaped wooden block which in turn is insulated from the steel plate by means of sponge rubber pads. The electric vibrometer is mounted directly on a heavy steel block. The design and construction of this equipment eliminates external vibrations and well insulates the generator from the vibrometer.

Conclusion

Certain devices and means have been shown which are definite aids to quality control in the manufacture of aircraft generators. New problems will require new aids. This is but one of the many tasks of the engineer today.

Behavior Factors of Rectifier-Driven D-C Motors

VICTOR SIEGFRIED
MEMBER AIEE

Synopsis: In the rapid development and application of rectifiers to drive d-c motors, certain factors which influence the behavior of the motor may require unaccustomed consideration. The pulsating nature of currents which may be encountered introduces departures from conventional treatment of (1) heating value of the current waves, (2) stresses upon the insulation and commutator resulting from high peaks of voltage involved, and (3) torque pulsations tending to produce vibration and noise.

Analysis is made from experimental and theoretical approaches of possible ratios of the heating values to be expected with typical wave shapes applied to electronically driven motors. The ratio of effective to average values of current varies with the load on the machine and with the number of tubes used in the rectification process. It is shown that the problem is most acute in machines of small rating where mechanical inertia is low and where tube capacities would permit the use of but one or two tubes for economy of materials. This situation may require "overmotoring" an application in order to allow the required thermal capacity. Vibrations which will otherwise show up in undesirable effects on the driven machine, and noise, would also dictate the use of larger machines than necessary with conventional d-c practice.

The conclusion is drawn that factors not at first apparent must be considered in the application of the rectifier drive, and that as many phases as practicable are desirable in the rectifier.

THE electronic rectifier is currently achieving considerable prominence as a source for d-c motors.[1–5] It has many advantages over both fixed- and variable-voltage generator systems in the reduction of critical materials and elimination of rotating parts in the conversion process. Furthermore, its ease of control by means of the grid-phase-shifting system of the thyratron or the ignitron suggests many applications where new results can be secured.

In the use of phase-controlled rectifier sources for d-c motors, considerable attention must be given to the wave shapes of the currents supplied to the machine and their effects on it. Whereas continuous and direct currents have long been considered synonymous, there is introduced a new concept of *direct* current which may be pulsating but still continuous or even may be discontinuous for large fractions of a cycle. These differences from the continuous and substantially steady values encountered in conventional d-c practice produce far-reaching and sometimes surprising effects. They show that the rectifier is not inherently a panacea for all ills, but rather introduces problems of its own which can be met and conquered if recognized.

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Nature of Problems

The controlled rectifier is inherently a phase-angle-control device which delays the beginning of current flow to a predetermined point in the cycle of alternating voltage applied to the tubes. For single-tube half-wave rectifiers, the result is pulses of current which can never be continuous but will always be less than one-half cycle in length. With two tubes in a full-wave rectifier, the current produced will be pulsating but seldom can be continuous. In polyphase systems with three or four tubes the voltages will overlap and the currents can become continuous when the rectifier is turned full on. With six or more phases, the possibility is reduced that phase control will produce discontinuous currents for any but the lowest of output levels. With sufficient number of tubes, the current pulses of the individual tubes are smoothed out, and the resultant current approaches the steady value of generator-produced current.

Thus the region in which these problems are encountered is confined to rectifiers using only a few tubes and in which the output is either discontinuous or else contains an appreciable ripple component.

There are a number of methods, inherently expensive, for filtering this output to make it smooth by the time it reaches the motor. Combinations of inductance and capacitance are required. Inductance in the machine may help as in the field circuit, where the machine is somewhat self-filtering. In the armature circuit, however, a high inductance interferes with commutation and is to be avoided.

Effects on Motor

There are certain factors which must be considered in analyzing the behavior of d-c motors when subjected to currents of this type.

1. The current produced by the rectifier is pulsating. It may be continuous but pulsating to a high degree, or it may even be zero for a portion of the cycle and be discontinuous, which is the more aggravated situation.

2. In order to achieve a required average value of current, this pulsating current must have peaks which are much larger in value. This results in an effective or root-mean-square current much larger than indicated by d-c ammeters.

3. The heating value of these pulsating waves is much greater than for the equivalent amount of conventional direct current. The resistance takes on an effective value which is larger due to stray-load-loss components, and the high peaks of current involved thus make the total I^2R loss far greater than ordinarily anticipated.

4. Peaks of voltage in the armature will be encountered due to the interaction of the pulsating current and the inductance of the armature and anode transformer circuits. These give a voltage wave shape in which the d-c value is almost unrecognizable, with peaks three or four times as large as the voltmeter value. These voltages endanger the armature and commutator insulations, and in some cases, commutator sparking is a direct consequence of the instantaneous peaks.

5. Torque produced by the machine becomes pulsating, with its attendant noise and vibration problems. These may be reflected in unsatisfactory performance of the driven machine, especially at light load or low speeds.

Analysis of Current Output

With current wave shapes as variable as occur in these rectifier drives, no one method of attack fits all cases. Where current is continuous but pulsating, it may be analyzed much as an average value with superimposed ripples of high frequency. The fundamental of this harmonic will be the supply frequency times the number of phases involved.

For discontinuous waves, the effect on the machine is more serious. Fortunately the analysis is quite simple due to the fact that the armature current pulse approximates a half sine wave, regardless of the portion of the cycle which it occupies. This is due to the influence of the inductance of the circuit, tending to hold the current rise down, and main-

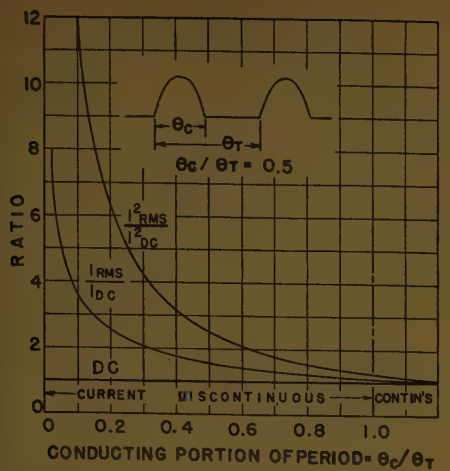


Figure 1. Effect of angle of conduction on heating and current ratios

Insert: Example of wave shape when $\theta_C = 0.5\theta_T$

tain current flow past the voltage zero. Vedder and Puchlowski[4] have given good detailed analyses of this, but for purposes of this paper, the approximation to the half sine wave is adequate. In the following analysis, it is to be re-emphasized that the range in which more serious differences occur is at light loads and low speeds, depending on specific applications of the drive.

The main comparison of heating values, the most important difference, is in that of the average and effective values. The peak value of current, I_m, is used as the comparison figure in each case. For any given sine-wave pulse of half-cycle value as in Figure 1, the average value over that time is $2I_m/\pi = 0.636\ I_m$ and the effective value is $0.707\ I_m$. In averaging the current over a complete cycle, it will be in proportion to the ratio of angle of conduction, θ_C, to the total cycle, θ_T, but the effective value will be the square root

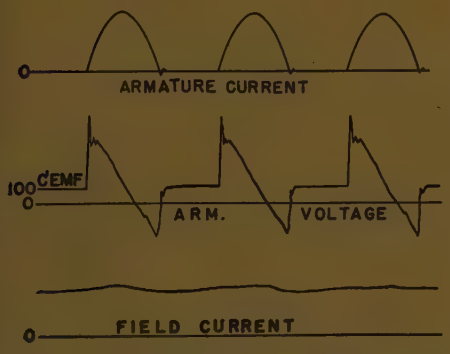


Figure 2. Oscillograms of machine currents and voltage for $\theta_C = 0.53\ \theta_T$

Armature current = 1.55 amperes root-mean-square and 1.09 amperes direct current
Armature voltage = 147 volts root-mean-square and 100 volts direct current
Field current = 0.360 amperes root-mean-square and 0.340 amperes direct current

Table I. Effective and Average Current Ratios for Discontinuous Currents

Conducting Portion of Period, θ_C/θ_T	D-C (Average) Values		Root-Mean-Square (Effective) Values		Current Ratio, I_{RMS}/I_{DC}	Heating Ratio, I_{RMS}^2/I_{DC}^2
	$\times I_{max}$	$\times I_{max}^2$	$\times I_{max}$	$\times I_{max}^2$		
0.10	0.064	0.004	0.223	0.050	3.49	12.5
0.25	0.159	0.025	0.354	0.125	2.24	5.0
0.33	0.212	0.045	0.407	0.166	1.92	3.70
0.50	0.318	0.101	0.500	0.250	1.57	2.49
0.67	0.424	0.180	0.577	0.333	1.33	1.85
0.75	0.477	0.228	0.612	0.375	1.28	1.65
0.90	0.572	0.327	0.671	0.450	1.17	1.38
1.00	0.636	0.405	0.707	0.500	1.11	1.23
Multiphase	1.0	1.0	1.0	1.0	1.0	1.0

Note: Table based on $I_{max} = 1.0$, and $R_{eff} = R_{d\text{-}c}$. Specific application of these data to rectifier-driven motors requires determination of actual current wave shape for each load and operating speed adjustment.

of the average square. For the case shown in Figure 1 where the current flows for one half the time, the effective value will be 0.5 I_m, whereas the average (d-c) value will be 0.318 I_m, a ratio of 1.57 to 1. Thus the true heating I^2R value of the wave will be 0.25 I_m^2 whereas that indicated by a d-c instrument will be $(0.318I_m)^2$ or 0.101 I_m^2, a ratio of $2^1/_2$ to 1. In the case of current for one-third of a cycle, the ratio is even more striking, as the heating rate is 3.7 times that of the equivalent smooth direct current. Relationships for other conduction periods are shown in Table I and Figure 1. These also show that for the limiting case of multiphase operation, these ratios become 1.0, and average and effective values are equal.

A typical oscillogram of voltage and currents in an actual full-wave rectifier-driven machine is shown in Figure 2. Conduction is for 53 per cent of the half cycle. The values of effective and average values are shown, and agreement is quite close to the anticipated values from Figure 1 for this setting of the control. This indicates that the sinusoidal approximation is in quite close agreement to actual conditions.

Such an example will apply equally well to any number of phases in a rectifier if the conduction period is one half of the portion of the cycle assigned to each phase. Thus, the case shown would be like a half-wave circuit conducting 180 degrees per anode, or a full-wave circuit conducting 90 degrees per anode, or a three-phase circuit conducting 60 degrees per anode, and so forth.

Results of Analysis

When, as in the examples shown, current is operating in the discontinuous range, the motor heating rate due to copper loss alone will be excessively large for the intended rating. The remedy would appear to be to figure the duty cycle carefully to include these effects, or simply allow a step-up in the motor size to provide a safety factor. The obvious alternative is to allow the motor to run hotter and hope that the life of the insulation will be satisfactory.

Another factor which contributes to the machine's running hotter is the core loss. While the current may be self-smoothing to a large degree, there is a high-frequency flux pulsation which will occur where there is usually no impediment to eddy-current losses. This would indicate the desirability of laminated core and pole pieces.

Two other factors which combine against the life of the insulation are vibration and voltage stresses. The probable life of the insulation seems doomed to be shorter than for conventional motors regardless of heating. The combination of excessive voltage strains, chafing under vibration conditions and higher temperatures indicates that much more or better insulation must be built into the machine's design.

The remedy really lies in careful engineering of an application. The load can be figured to keep the motor working in the continuous current portion of the rectifier operation. Multiphase rectifiers can be used, so as to approximate the steady current of a generator as nearly as economically feasible.

Conclusions

This analysis of the application of phase-shift-controlled rectifiers to d-c motors leads to these conclusions:

1. Consideration must be given factors not usually encountered in d-c machine practice.
2. These factors are most acute in machines of small rating where the mechanical inertia is low and the load requirements justify the use of only one or two tubes.
3. Heating of the motor will be increased by the use of rectified currents over conventional direct current, especially if machine currents are discontinuous.
4. Vibration will be especially prominent at low speeds.
5. Excess heating and vibration become of

A Unit for Balancing the Voltages of Three-Phase Aircraft Inverters Supplying Large Single-Phase Loads

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Synopsis: A means of reflecting a single-phase load so it appears as a balanced three-phase load to the power supply is discussed in this paper. A balancer of this type, designed for use in aircraft, is described and its characteristics discussed.

Background

THE increase in electrically powered and electrically controlled equipment on aircraft has made it necessary, in certain cases, to add sizable single-phase loads to three-phase inverters. The nature of the existing three-phase load, however, placed severe restrictions on the permissible voltage unbalance. The need was felt for a means of maintaining balanced voltages between phases of a three-phase inverter operating under the conditions described above. As this balancer was to be used on aircraft, the further limitations that it be light in weight and efficient in operation were imposed.

In February 1943 a request was made by the Army Air Forces at Wright Field for a balancer of this nature. The single-phase load specified was approximately 265 volt-amperes, 115 volts, 94 per cent power factor, 400 cycles. The power supply was a three-phase inverter rated at 750 volt-amperes, 115 volts, 400 cycles.



Figure 1. The unit described in this paper rated 265 volt-amperes, 115 volts, 94 per cent power factor, 400 cycles

The Balancer

Several methods of attaining the desired results suggested themselves. The one decided upon[1] resulted in a unit weighing 1.5 pounds and measuring $3^1/_2$ by $2^3/_4$ by $2^3/_4$ inches. This balancer has an efficiency of 96 per cent at rated load and will supply this load continuously with a temperature rise of 40 degrees centigrade at sea level. The voltage unbalance of the inverter at rated single-phase load is negligible when the balancer is used. It consists of a tapped reactor and a capacitor connected to the line as in Figure 2.

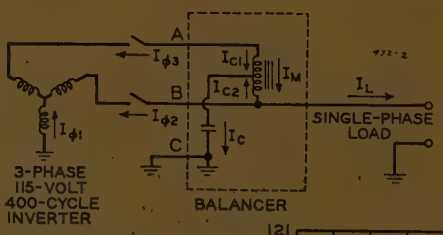


Figure 2. Schematic connection diagram for the balancer unit

Performance

The three-to-one phase balancer is designed for a particular single-phase load and will function ideally only at this load. Figure 3 illustrates this point. This characteristic of the balancer makes it advisable to incorporate the unit as part of the load and connect it to the power supply when the single-phase load is applied.

All of the accompanying data were taken using a Holtzer-Cabot inverter having the rating mentioned above for the three-phase output circuit. The inverter used had an initial voltage un-

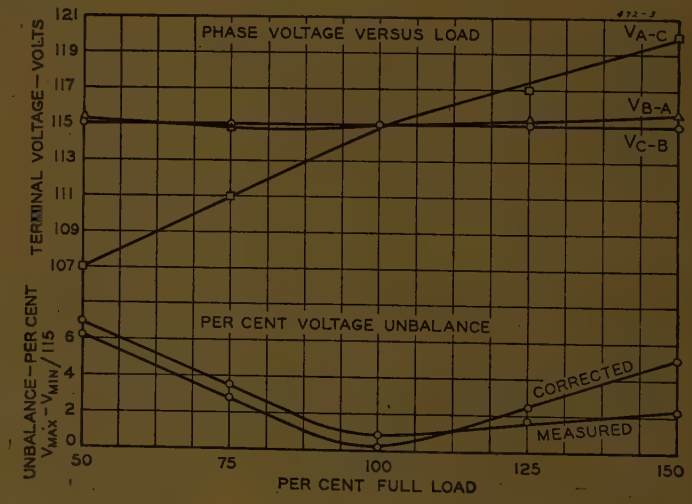


Figure 3. Voltage unbalance of the power supply under conditions of varying single-phase load. Balancer unit connected

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smaller importance as the number of tubes (phases) in the rectifier is increased.

6. Multiphase rectifiers appear to have definite electrical and mechanical advantages which will offset their increased cost and size.

7. Designs of machines must be altered to take account of current, torque, and flux pulsations superimposed on conventional d-c values.

References

1. THYRATRON CONTROL OF D-C MOTORS, G. W. Garman. AIEE TRANSACTIONS, volume 57, 1938, July section, pages 335–42.

2. THYRATRON MOTOR CONTROL, E. E. Moyer and H. L. Palmer. AIEE TRANSACTIONS, volume 62, 1943, November section, pages 707–11.

3. ELECTRONIC CONTROL OF D-C MOTORS, K. P. Puchlowski. AIEE TRANSACTIONS, volume 62, 1943, pages 870–7.

4. THEORY OF RECTIFIER—D-C MOTOR DRIVE, E. H. Vedder and K. P. Puchlowski. AIEE TRANSACTIONS, volume 62, 1943, pages 863–70.

5. INVERTER ACTION ON REVERSING OF THYRATRON-MOTOR CONTROL, H. L. Palmer and H. H. Leigh. AIEE TRANSACTIONS, volume 63, 1944, April section, pages 175–84.

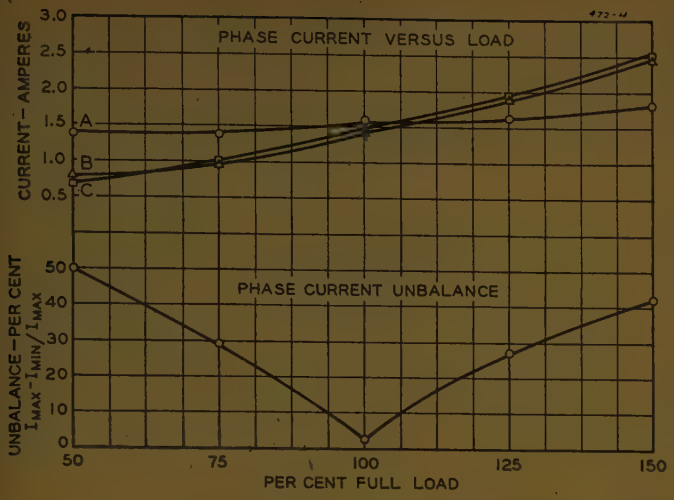
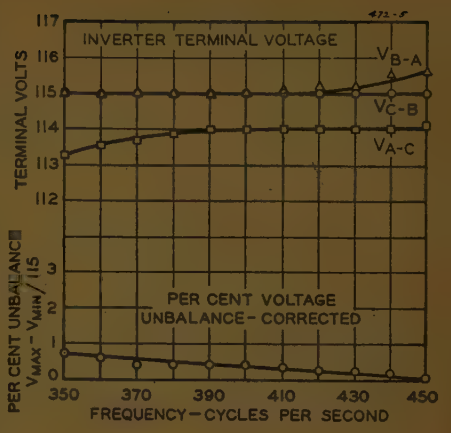
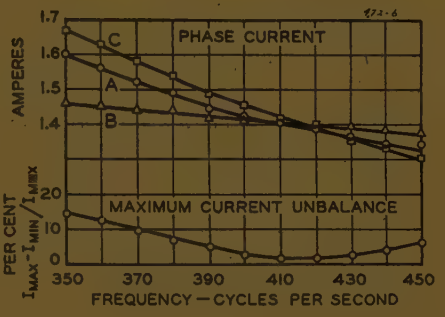


Figure 4 (left). Phase-current unbalance of the power supply under conditions of varying single-phase load. Balancer unit connected

Figure 5 (right). Voltage unbalance of the power supply versus frequency. Balancer unit connected

Figure 6 (below). Phase-current unbalance of the power supply versus frequency. Balancer unit connected





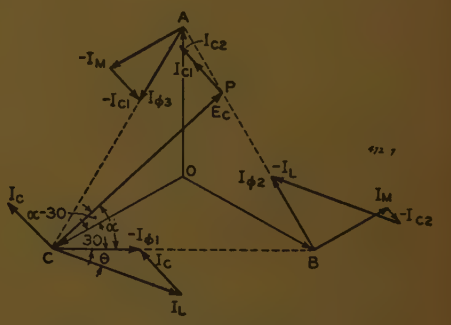


Figure 7. Vector diagram of the unit

balance of 0.8 volt at no load. The curves marked "corrected" have been compensated for this unbalance.

Since the unbalance of terminal voltage with unbalanced load is a function of the inverter, curves of phase-current unbalance have also been plotted to give a clearer picture of the behavior of the balancer.

The voltage across the load was kept constant at 115 volts so that the data would not be confused by the regulation of the inverter.

For the purpose of comparison, the performance of the inverter alone is given. At 100 per cent single-phase load on terminals *B* and *C*, with no balancer, the inverter voltages were unbalanced 7.65 per cent. The readings were:

V_{C-B}	V_{B-A}	V_{A-C}
115.0 volts	116.0 volts	123.8 volts

The frequency response of the balancer is also shown (Figures 5 and 6). It can be seen from these curves that the unit operates reasonably well over a wide range of frequencies, so that the value of the unit is not impaired by the speed regulation of the power supply.

Conclusion

The unit described is suitable for aircraft use due to its small size, light weight, and high efficiency. It has the added advantage of improving the power factor in the lines to 0.866 in those cases where the single-phase load is less than this value. Additional tests prove the unit will function at temperatures and pressures encountered at high altitudes.

Appendix

The operation of the balancer can be understood by referring to the vector diagram of Figure 7. The single-phase load is connected across terminals *B* and *C* of the inverter. The balancer is connected so that the reactor is across phases *A* and *B* and the free end of the capacitor is connected to terminal *C*. Phase rotation is *A–B–C*.

The value of the capacitor and the reactor needed to balance any single-phase load as well as the position of the reactor tap, can be obtained by referring to the schematic wiring diagram (Figure 2) and the vector diagram (Figure 7). The vector diagram is drawn to represent balanced conditions.

From Figure 2:

$$I_{\phi 1}=I_L+I_C \tag{1}$$

$$I_{\phi 2}=I_M-I_{C2}-I_L \tag{2}$$

$$I_{\phi 3}=-I_{C1}-I_M \tag{3}$$

Under balanced conditions

$$I_{\phi 1}=I_{\phi 2}\underline{/120^\circ}=I_{\phi 3}\underline{/120^\circ} \tag{4}$$

and

$$V_{OC}=V_{OB}\underline{/120^\circ}=V_{OA}\underline{/120^\circ} \tag{5}$$

Assuming no power lost in the balancer and the phase currents in phase with the terminal-to-terminal voltages (a condition found necessary to obtain balance), the power supplied by the inverter must equal the power absorbed by the single-phase load.

$$3E_\phi I_\phi \cos 30^\circ = E_L I_L \cos\theta \tag{6}$$

or

$$\sqrt{3}E_L I_\phi \times 0.866 = E_L I_L \cos\theta \tag{7}$$

and

$$I_\phi = {}^2/_3 I_L \cos\theta \tag{8}$$

From equation 1

$$I_C = I_{\phi 1} - I_L \tag{9}$$

The angle of the voltage across the capacitor is assumed 90 degrees lagging the capacitor current. The absolute value of capacitor voltage is:

$$/E_{CP}/ = (115 \sin 60^\circ)/\sin(60+\alpha) \tag{10}$$

and the voltage E_{BP} across the lower part of the reactor is

$$/E_{BP}/ = (115 \sin \alpha)/\sin(60+\alpha) \tag{11}$$

Making use of the superposition theorem, the net current through the reactor can be considered as being made up of the reactor magnetizing current I_M, a part of the capacitor current I_{C2} flowing between the reactor tap and terminal *B*, and the remainder of the capacitor current I_{C1} flowing between the tap and terminal *A*. It can further be seen that if the internal impedance of the power supply is small compared to the impedance of the load

$$I_{C1}/I_{C2} = E_{BP}/E_{PA} \tag{12}$$

The reactor magnetizing current is, from equation 2,

$$I_M = I_L + I_{C2} + I_\phi \tag{13}$$

The balancer described in the beginning of this article consisted of a four-microfarad capacitor and a reactor drawing a magnetizing current of 1.41 amperes tapped at 33.6 volts from the *A* terminal.

Reference

1. Letters to editor, *Electrical World*, December 26, 1942.

Vibration Protection for Rotating Machinery

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Synopsis: Vibration protection becomes desirable for rotating machinery when it is operated with certain defects. Electric generators may be operated with a single "ground" on the rotating field circuit without harm but the potential hazards of a second "ground" at another point on the same circuit are so great that the need of protective equipment for automatic disconnection of the generator is evident. Double "grounds" on a rotating field circuit may short-circuit a large part of the winding, unbalance the magnetic field, and set up vibrations of such serious magnitudes that the machine may be wrecked.

A method for obtaining satisfactory vibration protection for rotating machines has been developed. It uses equipment most of which is already on hand or readily available to most companies operating important machinery. The only part requiring construction is an electronic amplifier.

The equipment, its operating characteristics, wiring, and performance are described in this paper.

The described protective equipment has been in service on one machine since the latter part of 1942. It has given entirely satisfactory service. Another machine was protected in a similar manner for about three months until it was found convenient to rewind the field.

ROTATING machinery of all types is subject to damage when exposed to severe vibration caused by mechanical unbalance, or its equivalent, in the rotating element. Although electrical machinery has a good record in this respect, its freedom from vibration has been due largely to the degree of maintenance which it usually receives. Now, however, due to wartime restrictions, electrical and other machinery is often run under conditions which would not be permitted normally until after repairs had been made. When this is done, protective measures should be adopted where possible to prevent serious machine damages if the weakened part should fail.

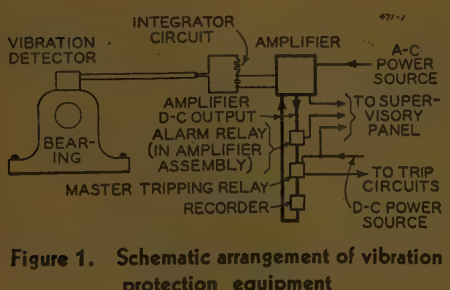


Figure 1. Schematic arrangement of vibration protection equipment

One expedient being adopted, as the occasion demands, is to continue operating a-c synchronous machinery after a "ground" has developed in the field winding of the rotor. Since the excitation system is operated ungrounded normally, the winding-to-ground fault is not harmful and does not prevent continued and normal machine operation. Such operation does, however, expose the machine to severe damage if a second "ground" should occur in the field winding or at any other place on the same excitation system. Two "grounds" may effectively short-circuit a portion of the field winding, thereby causing magnetic unbalance in the rotor. If a large portion of the field winding is short-circuited by the second "ground," the field current in the remainder of the winding will increase and further aggravate the unbalanced magnetic condition. Cases are on record of double "grounds" in field windings which have caused such severe vibration that the bearing pedestals were broken allowing the rotating field to drop on the stator laminations. Such failures have, in each known case, caused damages which required costly repairs and long machine outages.

A protective method, that detects excessive vibration, has been developed by engineers of the Consolidated Edison Company of New York. It operates relays which function quickly to initiate the circuit breaker operations required to relieve the condition. In the case of synchronous machinery the a-c armature and d-c field breakers are opened as fast as possible and, if desired, the prime mover power may be shut off. Other types of machinery would be handled as conditions and equipment permit.

Protective System

The protective system consists of the following parts:

1. Seismographic type vibration detector.
2. Integrator circuit.
3. Resistance coupled amplifier.
4. Alarm and protective relays.
5. Vibration recorder.
6. Supervisory circuits.



Figure 2. Seismographic vibration detector with cover removed

The arrangement of these parts is shown schematically in Figure 1. An alternating voltage is generated by the vibration detector when vibration is present in the part on which it is mounted. This voltage is impressed, through an integrator circuit, to an amplifier. The current is amplified and then rectified before being passed through the relays and the recorder. The system is provided with various features which prevent incorrect performance of the relays due to wire failure, tube failure, and loss of a-c or d-c power supply.

Vibration Detector

The vibration detector was one originally furnished by its manufacturer for use in connection with vibration recorders. It is constructed on the seismograph principle, with a strong permanent magnet supported on springs serving as the stationary element. A light coil supported to the base of the detector is free to move in the magnetic field when the base of the detector is subjected to vibration. The base of the detector is mounted rigidly on the bearing or other part whose vibration is to be measured. The detector is inherently a velocity measuring device since the alternating voltage it generates is directly proportional to vibration frequency and to vibration amplitude for sinusoidal vibration.

The detector is shown without its cover in Figure 2. Figure 3 shows two detectors mounted on a generator-outboard-bearing cap. Generator-bearing pedestals

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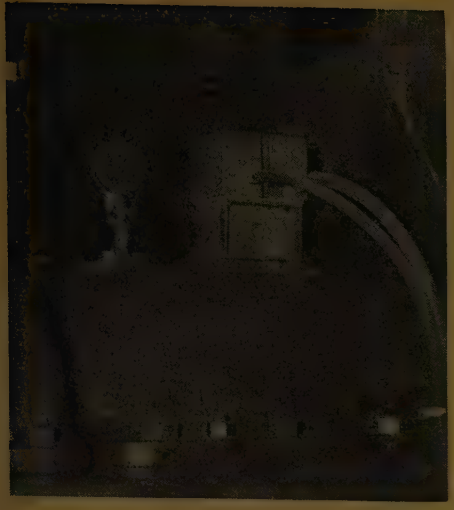


Figure 3. Vibration detectors mounted on turbogenerator bearing

are frequently insulated from the supporting bedplate of the machine. Care should therefore be exercised to prevent short-circuiting this insulation when installing metallic conduit for connection to the detector device mounted on the bearing cap. The detector mounting must be rigid enough to avoid resonant vibration in the detector supports. Since the wiring from the detector to the amplifier requires protection against induced voltages, twisted and shielded conductors are used. It is also preferable to have them routed remotely from other circuits carrying alternating current or impulse currents.

Integrator Circuit

Since it is desired to operate the protective relay on vibration amplitude only, it is necessary to interpose an integrator circuit between the detector and the amplifier. On the installation being described, this circuit consists of a 19,000-ohm resistor in series with a three-microfarad capacitor. The output from the detector is connected across the complete circuit while the voltage drop across the capacitor is wired to the amplifier and is used to govern the operation of the relays. The resistance of the circuit predominates over the capacitance, except at very low frequencies, hence the current in the circuit remains nearly constant with the same impressed voltage for any frequency of vibration at which protection is desired. Since for a given vibration amplitude the voltage generated in the detector is directly proportional to frequency while the capacitive reactance of the capacitor is inversely proportional to frequency it follows that the voltage drop across the capacitor does not change with different frequencies and is therefore proportional to vibration amplitude alone. The output voltage E_2 of the integrator circuit may be calculated for any input voltage E_1 and frequency f by use of the formula:

$$E_2 = \frac{E_1}{(19{,}000^2 + X_c^2)^{1/2}}$$

where

$$X_c = \frac{10^6}{2\pi f \times 3}$$

The circuit produces a voltage output nearly proportional to vibration amplitude over a wide frequency range. One curve of Figure 4 shows the degree of accuracy obtained with the circuit. At 30 cycles per second and above the error is negligible. At five cycles per second the output of the circuit is about 12 per cent low. Five cycles per second corresponds to about 16 per cent of rated speed for this machine.

Amplifier

The amplifier is resistance coupled so that it also will provide good accuracy over a wide frequency range, and particularly at the lower vibration frequencies. The degree of accuracy obtained in the amplifier at different frequencies is shown by the other curve of Figure 4. Reasonably good accuracy at low frequencies is desired to provide satisfactory vibration protection while machines are being brought up to speed or slowed down after a run. An over-all error of 15 per cent at five cycles per second, 12 per cent for the integrator circuit, and 3 per cent for the amplifier, is considered satisfactory for the protective relays since damage due to vibration is not likely to be severe at lower speeds. Some generators, such as cross-compound units, must have their field windings energized at the time of start-up and would, therefore, be subject to vibration due to double field "grounds" even at moderate speeds.

The over-all voltage amplification of the amplifier is about 1,700. Its response is linear from zero to 16 millivolts input from the integrator. The d-c load circuit resistance is 1,700 ohms. At 20 milliamperes, the output of the amplifier is 34 volts direct current and 0.68 watt.

Three stages are used in the amplifying circuit, two stages being obtained in two double-triode tubes and a third stage being obtained in two triode tubes connected in push-pull arrangement. To prevent incorrect relay operation in case of electron-tube failure, the filament circuits of the various stages are connected in series with an instantaneous overcurrent relay. When the filament current is reduced due to any filament failure the relay operates to open the output circuit of the amplifier and to close a signal circuit. Such operation makes the protective relays inoperative and starts a supervisory lamp flashing at a point where it will attract the attention of the operators.

The amplifier output is rectified by a copper-oxide bridge circuit and the direct current from the rectifier is wired to the relays, and to the recorder, if used.

Figure 5 shows a schematic diagram of the amplifier connections and associated equipment. The over-all calibration of the vibration-protection equipment including the detector unit, integrator, and amplifier is given in Figure 6. To obtain this calibration initially the detector was caused to vibrate and its double amplitude of vibration in mils was measured with a portable Starrett type vibrometer. The range of d-c output current as obtained from the amplifier for various values of vibration is entirely suitable for relay application. It is not practicable to check the over-all calibration (d-c output current versus mils vibration) each time the amplifier and relay equipment require calibrating. Since there are occasions when the amplifier must be recalibrated (when tubes are changed or when other parts are replaced) a 60-cycle calibration curve was obtained following the initial over-all calibration. This curve is also given in Figure 6. The calibration curve was obtained by impressing 60-cycle voltages across the same input terminals to which the output of the detector is connected. This calibrating curve assists not only with adjustments to the amplifier and relays but permits a complete check on the accuracy of the recorder, as required.

It will be noted on Figure 5 that the amplifier circuits are grounded through a 0.5-megohm resistor. This resistance is intended to prevent incorrect operation of the relays in case a wire "ground" should develop in the detector output circuit at a point where the "ground" potential is operating at a small voltage difference

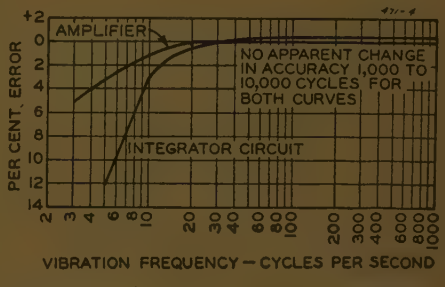


Figure 4. Accuracy curves for integrator and amplifier versus vibration frequency

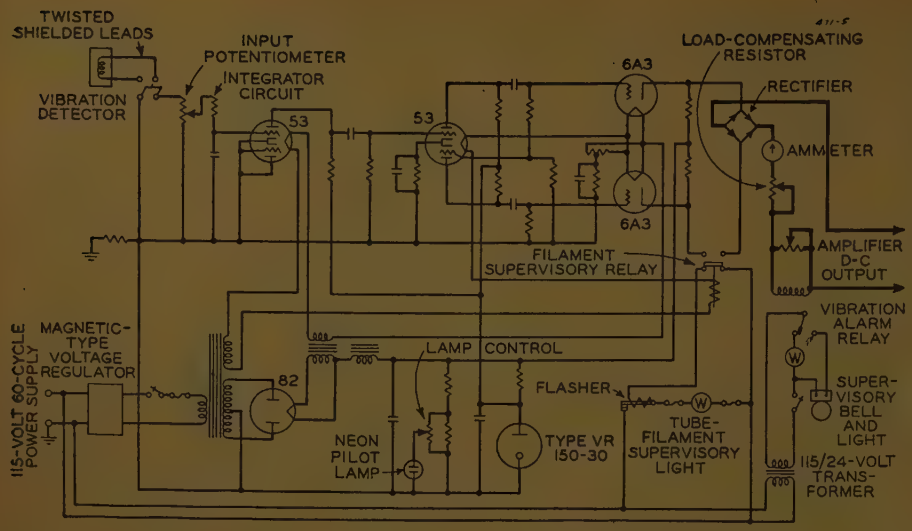


Figure 5. Connection diagram for amplifier and associated equipment

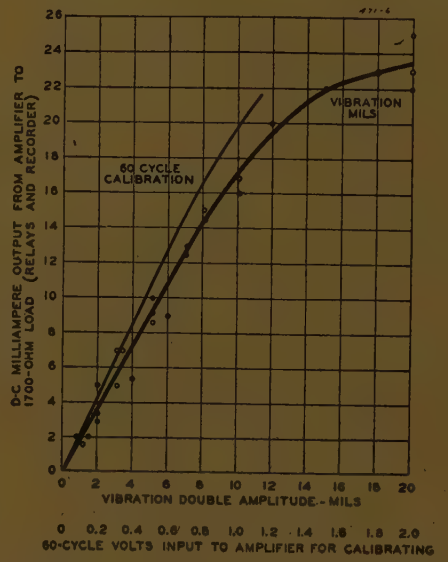


Figure 6. Characteristic output curves of amplifier

with respect to the "ground" bus potential at the amplifier. A potential of about one volt at 60 cycles between the two wires from the detector is sufficient to cause operation of the master tripping relay.

Relays

A diagram of relay connections is given on Figure 7. Two "lock-out" relays are used, one being an a-c undervoltage relay and the other a d-c undervoltage relay. These undervoltage "lock-out" relays open the tripping circuit instantly if either source of power is lost. The a-c relay will reset about one minute after restoration of voltage which allows ample time for the tubes of the amplifier to become heated and its output current to become steady before the equipment is returned to service. The d-c undervoltage relay resets in about 10 seconds after voltage is restored, this time allowing for the master tripping relay element to come to its stable position after its field coil is energized.

The alarm overcurrent relay is a simple hinged armature device equipped with a variable resistor connected in parallel with its coil to provide means for adjusting its operating current value. A second resistor is connected in series with the relay circuit to permit readjustment of the over-all series-circuit resistance when any variation is made in the resistance in parallel with this relay coil. This permits retaining a constant resistance in the output circuits from the rectifier. The relay for this installation is adjusted to give an alarm to the operators at eight mils vibration.

The master tripping relay has a standard d'Arsonval type element with a field coil. Its field coil is connected to a separate d-c power supply. The d-c output from the rectifier is passed through the moving coil of the relay. Since the moving element is restrained by a small spiral spring the element changes its position to follow any increase of vibration. Adjustment is made by changing the position of the stationary contact so as to require more or less travel of the moving element against the restraining spring. Since the relay moves to follow any change in vibration almost instantly, it will be in position to trip shortly after the machine vibration reaches the value for which the relay is set to operate. The master tripping relay trips the main generator breakers and the field breakers when the bearing vibration reaches 12 mils.

Any d-c relay which will operate over the range of current shown in Figure 6 would be suitable for the master tripping relay. Of course the vibration scale can be contracted or expanded on Figure 6 by simply varying the potentiometer adjustment at the input to the integrator. The relay used should be fast to prevent an excessive overswing of the vibration before the tripping relay operates.

The relays become inoperative but do not operate incorrectly due to the following troubles: (1) open-circuited or short-circuited wires from the vibration detector, (2) failure of electron tubes, (3) interruption of a-c or d-c power supply, (4) relay failure, or (5) tripping-circuit failure. Complete supervision is provided to the operators for conditions (2), (3), and (5).

Figure 8 shows the relay panel with the amplifier at the top. It will be noted that two complete protective equipments are provided. The two corresponding vibration detectors are shown in Figure 3. On this installation two complete protective equipments were installed with separate power sources and tripping circuits to provide a high degree of reliability. In case any part of one equipment develops trouble the second equipment will provide complete vibration

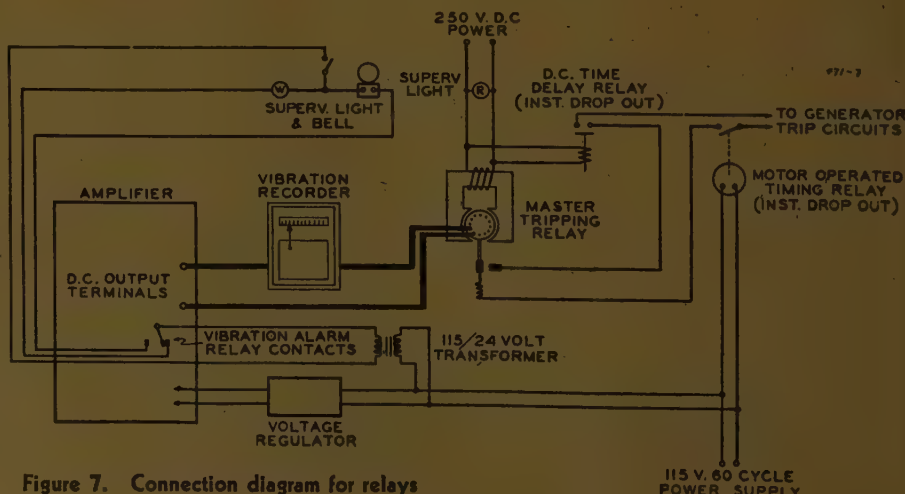


Figure 7. Connection diagram for relays

protection since all of its parts function independently. Since this particular generator is one of the largest on the system it was decided that the unit should not be run at any time without at least one vibration protective equipment in service. Two equipments have the advantages of permitting tube changes and calibration work on the parts of one without having to take the main generator out of service and they provide a continuous check on the accuracy of each other.

Vibration Recorder

A recorder may or may not be used in such a circuit but on the installation being described a recorder is found useful and it is inserted in series with the d-c output from the rectifier. The instrument is located near the turbine to permit continuous supervision of the bearing vibration by the turbine engineer. A continuous record is obtained of bearing vibration in a manner not feasible with indicating instruments. Samples of such records when starting, when running, and when shutting down are shown in Figure 9. The recorder instrument is an ordinary low range d-c milliammeter.

Supervisory Circuits

A supervisory panel for the equipment may be located at any convenient point. In the installation being described this panel is placed above the main control panel for the generator. There are four signals:

The first is an alarm and a light operated by the vibration alarm relay at 8 mils vibration.

The second signal is a light which is normally energized. It starts to flash when an electron-tube filament burns out. Such action is initiated by the undercurrent relay which is in series with the amplifier filament circuit. Loss of filament current makes the protection inoperative.

The third signal is a light which is normally energized and which becomes dark when the d-c power supply is interrupted. In this installation, the d-c power is used only for polarizing the master protective relay, hence, loss of that supply makes the protective equipment inoperative.

The fourth signal is the lights of the first and second signal both of which become dark when the a-c power supply is interrupted. Loss of a-c power deenergizes the amplifier and makes the protective equipment inoperative.

Operating Experience

No uncontrolled or accidental outage of the equipment has occurred since it

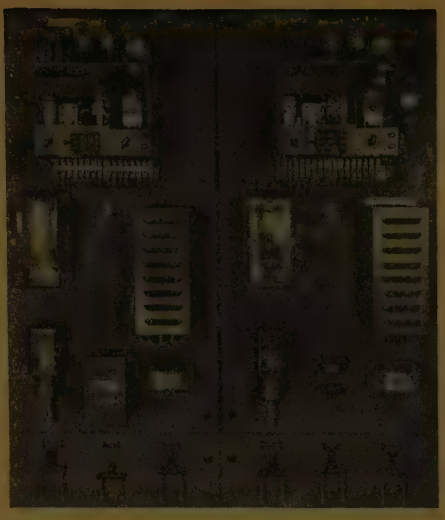


Figure 8. Vibration protection relay panels

was placed in operation in October 1942. It has not been called upon to disconnect the generator automatically since no severe vibration has occurred.

On one occasion a momentary "ground" occurred on one of the wires that connect to the vibration detector. Since the "ground" potential at that point was slightly different from the "ground" potential where the amplifier was mounted, this voltage difference, being impressed on the first stage of the amplifier, caused a surge of direct current to the relays and recorder. The recorder swung up-scale and the vibration alarm relay operated but the "ground" did not remain on the wire long enough for the master tripping relay to operate. The 0.5-megohm resistor was then connected in the "ground" circuit at the amplifier to prevent incorrect tripping due to an accidental "ground."

When first installed the vibration alarm relay was adjusted to operate at six mils vibration. It was found that the relay would operate occasionally as the generator was passing through its critical speed on starting or shutting down operations. The relay was readjusted to operate at eight mils vibration and no further relay operations of this type have been reported.

On two or three occasions the vibration alarm relay has operated and reset as the generator load was being increased. It was found that the machine experienced abnormal vibration of short duration as some of the steam valves are opened to increase the power output of the turbine. This behavior does not occur each time the load is brought up on the machine, in fact its occurrence is more "the exception" than "the rule."

Maintenance

Maintenance on the vibration protective equipment may be made to fit in with maintenance on other generator protective relays. The amplifier, relays and recorder should be calibrated on the same schedule with generator differential relays.

The amplifier tubes should be replaced every three to six months assuming they are in service continuously. The replacement tubes for the push-pull stage should be selected for balance before being installed.

The vibration detector should be inspected at times when the machine is shut down to determine whether any parts have become loose; once a month is often enough.

The usual attention to paper and ink must be given the recorder.

Conclusions

1. A simple scheme is made available for suitable and fast vibration protection for rotating machinery.

2. The protective scheme provides suitable protection against magnetic unbalances as well as against mechanical unbalance in rotating elements.

3. The equipment described has been in service since 1942 and has given a record of reliability entirely satisfactory for large turbogenerator protection.

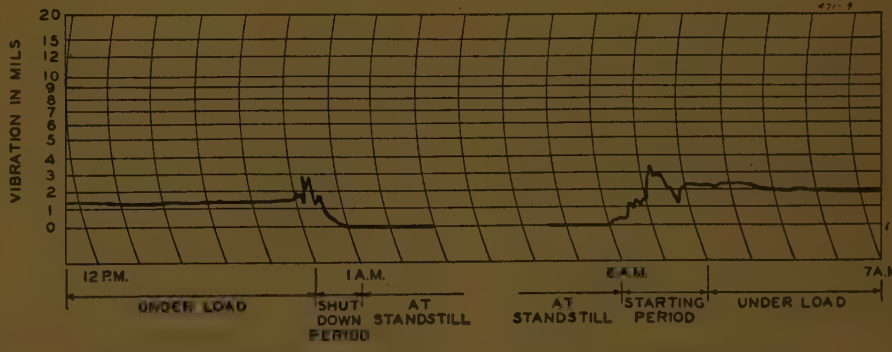


Figure 9. Typical vibration recorder charts for turbogenerator bearing

Measured Electrical Constants of 270-Mile 154-Kv Transmission Line

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Synopsis: Results of measurements are presented of fundamental electrical constants of a 270-mile 154-kv transmission line. Comparisons are made between values obtained from test and those obtained from circuit dimensions and computations, thus providing a check on some conventional methods of determining basic line impedance and admittance units. Included are unit resistance, inductive reactance, and capacitive susceptance together with ***ABCD*** constants and characteristic impedance of positive-phase sequence, zero-phase sequence, and one-conductor-and-ground long-line networks.

PART of results obtained from measurement of electrical constants of long transmission line have been presented previously.[1] Interest shown in these measurements and request for additional information led to this more complete presentation of data and results of tests which will be of interest to engineers who follow long-transmission-line theory and practice, relay application, and fault studies.

Engineers desire to check theory behind development of formulas and constants by actual measurement. Thus theoretical procedures which may include many factors and assumptions might be replaced, if possible, by more simple methods. Check measurements of zero-phase-sequence impedances especially are desirable, because calculations for these include effect of indefinite factors, such as, average distance between shield wires and conductors, their average height above ground, effect of earth-resistivity and reactance, and variations in construction from the straight-line structure. To a lesser degree perhaps, a check of positive-sequence values is also desirable to determine importance of assumptions used in theoretical calculations.

Since new-type impedance relays described previously[2,3] were being applied, and since accurate basic data for determining normal and short-circuit conditions were desired for satisfactory operation and protection, measurements of unit values were made upon completion of line but before it was placed in operation. These data obtained from measurements, also provided information to check relay settings previously computed from theoretical sources.

Specific methods of determining line constants are not recommended and a comparison between methods of computation is not made, but rather the paper is intended to present simply results of field measurements and to compare these with values obtained through some conventional procedures.

Line Construction

Typical structure used on straight-line construction is shown in Figure 1. Figure 2 gives average spacing and configuration of conductors and shield wires. Horizontal distances are same as those at typical straight-line structure and vertical distances are assumed as average considering that sag of shield wires is less than sag of conductor. Line is 268.52 miles long without intermediate switching stations or taps. Ruling span is 600 feet. It is single-circuit of wood-pole H-frame construction with horizontal conductor spacing of 14 feet 6 inches. Conductor is 0.683 inch in diameter and is 250,000 circular mils of hollow hard-drawn copper. At straight-line structures, it is supported by ten 10-inch suspension insulators having $5^3/_4$-inch spacing, and 15 insulators are used on grounded steel supports and dead ends. Two $^3/_8$-inch high-strength galvanized-steel overhead ground wires shield line. These have vertical clearance above power conductors of about 10 feet at structures and about 18 feet at mid-span. A steel cross member supports overhead ground wires and connects these to both pole grounds at each



Figure 1. Typical structure, 154-kv line

structure. Pole ground is number 6 steel wire carried down pole and wrapped around base. There are no counterpoises on line. Shield wires are connected to station grounds at terminals. There are three complete transposition barrels in line.

Test Procedure and Conditions

Measurements were made by applying potential from 2,400/4,160-volt 60-cycle source supplied from three 50-kva 13,800–2,400/4,160-volt Y-connected transformers at one terminal with conductors at other end appropriately connected for six short- and open-circuit tests listed in Table I. Portable ammeters, voltmeters, and wattmeters of good quality and recent calibration were used with outdoor instrument-type current and potential transformers at terminal at which potential was applied. Similarly ammeters and voltmeters were used at opposite terminal. All measurements include impedance of carrier-current wave traps

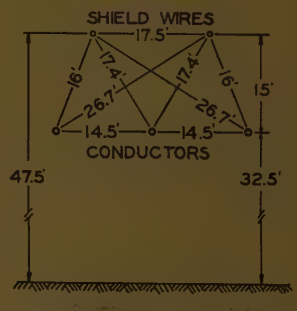


Figure 2. Conductor and shield-wire configuration. Approximate average spacings and heights above ground are shown

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which were in service as part of the communication system used throughout tests, but 60-cycle effect of these was negligible. Atmospheric temperature during five or six hours of testing ranged from 75 to 90 degrees Fahrenheit with perhaps 85 degrees as an average value.

Phase rotation of terminal systems was also compared using a phase-sequence instrument and found to check transposition plan. This was later confirmed when systems were synchronized first time. Open- and short-circuit impedances, ***ABCD*** constants, unit impedance and admittance values, and characteristic impedances were calculated from instrument readings of amperes, volts, and watts as discussed in Appendix I.

Summary of Results

Tables I to VII, inclusive, summarize data taken during tests; compare line constants obtained from these data with those obtained from computations; and list other related information. For brevity, word "measured" applies to values obtained through supplementary calculations from test data, and word "computed" applies to values obtained from references or from computations based on physical dimensions of line and on conductor and shield wire configuration. In following discussions, "reactance" refers to inductive reactance, and "susceptance" refers to capacitive susceptance. Symbols are explained in Appendix VI.

Table I lists impedances as viewed from instruments with conductors at other end open- or short-circuited to ground and as obtained from supplementary calculations using data from ammeter, voltmeter, and wattmeter readings. Also included are computed values of open- and short-circuit impedances obtained from ***ABCD*** constants in Tables II, III, and IV. Tests *A* and *B*, *C* and *D*, *E* and *F* give basic data for positive-phase sequence, zero-phase sequence, and one-conductor-and-ground networks, respectively. Test *B* was repeated with conductors short-circuited together at far end but not grounded. Instrument readings were same as before. There was no potential between short-circuited conductors and ground, indicating balanced impedance of conductors.

Table II indicates that positive-phase-sequence values of resistance and reactance are higher than computed values with susceptance being slightly smaller. Difference between measured and computed values may be result of not taking into account effect of ground and shield

Table I. Summary of Tests and Impedance Data

Test Description	Test	Symbol	Measured Impedances in Ohms as Viewed From Instruments R	$+jX_L$ or $-jX_C$	Z/θ	Computed
Balanced three-phase potential applied to three conductors with them open at other end	*A*	$Z_{ocp}=\frac{A_p}{C_p}$	21.2	$-j648$	648/88.13	646/88.03
Balanced three-phase potential applied to three conductors with them shorted and grounded at other end	*B*	$Z_{scp}=\frac{B_p}{A_p}$	85.8	$+j243$	258/70.6	251/71.48
Single-phase potential applied between all three conductors and ground with them tied together and not grounded at other end	*C*	$\frac{Z_{oc0}}{3}=\frac{A_0}{3C_0}$	21.2	$-j273$	275/85.6	270/84.29
Single-phase potential applied between all three conductors and ground with them tied together and grounded at other end	*D*	$\frac{Z_{sc0}}{3}=\frac{B_0}{3A_0}$	88.2	$+j232$	247/69.1	275/64.39
Single-phase potential applied between one conductor and ground with it not grounded at other end	*E*	$Z_{oc1}=\frac{A_1}{C_1}$	34	$-j707$	707/87.25	695/86.53
Single-phase potential applied between one conductor and ground with it grounded at other end	*F*	$Z_{sc1}=\frac{B_1}{A_1}$	150	$+j399$	426/69.4	430/67.9

Table II. Positive-Phase-Sequence Constants

$A_pB_pC_pD_p$ Constants

	A_p	B_p	C_p
Measured	0.853/3.19	220/73.79 ohms	0.001316/91.32
Computed	0.854/2.84	214/74.32 ohms	0.001332/90.87

Per mile values

	Resistance r_p, Ohms	Inductive Reactance jx_p, Ohms	Capacitive Susceptance jb_p, Micromhos
Measured	0.254 @ test temp	*j*0.823	*j*5.14
Computed	0.238 @ 25 deg C	*j*0.803	*j*5.18

Characteristic impedance

	$\sqrt{Z_{ocp}\times Z_{scp}}$	$\sqrt{Z_p/Y_p}$	$\sqrt{z_p/y_p}$	$\sqrt{\frac{A_pB_p}{C_pD_p}}$
Measured	409/8.77	409/8.58	409/8.58	409/8.77
Computed	403/8.28	403/8.25	403/8.25	403/8.28

wires in conventional computations and of angles in line and other factors causing departure from uniform construction. Characteristic impedance of 409 ohms is near 400-ohm value commonly accepted.

Table III shows that measured zero-phase-sequence resistance and reactance unit values are smaller than computed values which are based on small current flow of one or two amperes in shield wires believed to be comparable to that flowing during tests. During fault conditions, currents are high causing some change in resistance and reactance of shield wires. Assuming 40 amperes flowing in each shield wire, computed unit values are: $r_0=0.696$ and $x_0=2.44$. Susceptance values are same, possibly due to rather complete method of calculating referred to in Appendix V.

Table IV values for a one-conductor-and-ground circuit are of interest although they are not actually used in conventional high-voltage-transmission-line and system-fault calculations. Measured unit resistance is lower than computed. Reactance and susceptance values compare favorably. Computed values of resistance and reactance in this case, also depend on amount of current assumed flowing in shield wires. Of course, comparatively small currents flowed during measurements. Computed values listed assume about one or two amperes flowing. With 40 amperes assumed in each shield wire, computed unit resistance and reactance values are: $r_1=0.389$ and $x_1=1.35$. Characteristic impedance of 550 ohms measured at 60 cycles is of interest and compares with 500-ohm value commonly used for a single-wire and ground circuit and in design of lightning-proof lines. One-conductor-and-ground circuit measured forms part of carrier-current channel.

Table V summarizes characteristic impedances obtained from data taken during tests.

Table VI gives important ratios. Those obtained from measured data are lower except ratio of susceptances which are nearly equal.

Table VII gives ratios of sending-to-receiving current and voltages for the several tests. It will be noted that reciprocal of voltage rise ratios in tests *A*,

Table III. Zero-Phase-Sequence Constants

$A_0B_0C_0D_0$ Constants	A_0	B_0	C_0
Measured	0.735/6	545/75.1 ohms	0.000892/91.6
Computed	0.716/7.88	591/72.27 ohms	0.000885/92.17

Per mile values	Resistance r_0, Ohms	Inductive Reactance jx_0, Ohms	Capacitive Susceptance jb_0, Micromhos
Measured	0.636 @ test temp.	$j2.14$	$j3.65$
Computed	0.829 @ 25 deg C.	$j2.28$	$j3.65$

Characteristic impedance	$\sqrt{Z_{oc0} \times Z_{sc0}}$	$\sqrt{Z_0/Y_0}$	$\sqrt{z_0/\ _0}$	$\sqrt{\frac{A_0B_0}{C_0D_0}}$
Measured	783/8.25	782/8.28	782/8.28	783/8.25
Computed	816/9.95	816/9.95	816/9.95	816/9.95

Table IV. Constants of One-Conductor-and-Ground Circuit

$A_1B_1C_1D_1$ Constants	A_1	B_1	C_1
Measured	0.798/4.39	340/73.79 ohms	0.001130/91.64
Computed	0.795/4.9	342/72.8 ohms	0.001145/91.43

Per mile values	Resistance r_1, Ohms	Inductive Reactance jx_1, Ohms	Capacitive Susceptance jb_1, Micromhos
Measured	0.408 @ test temp.	$j1.295$	$j4.46$
Computed	0.438 @ 25 deg C.	$j1.3$	$j4.6$

Characteristic impedance	$\sqrt{Z_{oc1} \times Z_{sc1}}$	$\sqrt{Z_1/Y_1}$	$\sqrt{z_1/y_1}$	$\sqrt{\frac{A_1B_1}{C_1D_1}}$
Measured	550/8.93	551/8.75	551/8.75	550/8.93
Computed	548/9.32	547/9.32	547/9.32	548/9.32

C, and E, in which far-end conductors were not grounded, check very closely with the A constant as they theoretically should do for that particular arrangement of conductors. Reciprocal of current rise ratio of test B closely gives A constant. In this test conductors were tied together at far end and grounded, but as mentioned above, grounding had no effect on impedances measured. Also no current flowed through earth. However, reciprocal of current rise ratios in tests D and F do not give accurately the A constant. In tests D and F, conductors were grounded and current returned through ground and shield wires. This would indicate that unit values, particularly resistance and reactance, are slightly different for conditions with conductors grounded and not grounded in zero-phase sequence and one-conductor-and-ground circuit networks.

With no potential applied there was no indication of induced voltage on line when measured by potential transformers and voltmeters. During attenuation tests at carrier frequencies a two- or three-volt potential was observed between one conductor and ground using a vacuum-tube voltmeter shunted by 550-ohm resistor, indicating that any induced voltage effects were small. During test E in which

Table V. Summary of Characteristic or Surge Impedances

	Measured	Computed
From positive-phase sequence data. Conductor-to-conductor or conductor - to - equivalent neutral	409/8.77	403/8.28
From data taken with all three conductors tied together. Characteristic impedance between three conductors tied together and ground	261/8.25	272/9.95
Zero-phase-sequence characteristic impedance or three times preceding values	783/8.25	816/9.95
For one-conductor-and-ground circuit	550/8.93	548/9.32

Table VI. Ratios of Zero-Phase- to Positive-Phase-Sequence Resistance, Inductive Reactance, Impedance, and Capacitive Susceptance

	r_0/r_p	x_0/x_p	Z_0/Z_p	b_0/b_p
Measured	2.51	2.6	2.58	0.71
Computed	3.48	2.84	2.88	0.705

2,400 volts to ground was applied to one conductor only, potential between other two conductors and ground measured at far end was 300 volts. There was no appreciable unbalance of reactance between the three conductors as shown by three

Table VII. Current and Voltage Rise or Increase

Test	Rise: Current or Voltage at Energizing End Divided Into Values at Other End
A...Balanced three-phase potential applied to all three conductors with them open at other end	Voltage—1.18
B...Balanced three-phase potential applied to all three conductors with them short-circuited and grounded at other end	Current—1.2
C...Single-phase potential applied between all three conductors and ground with them tied together and ungrounded at other end	Voltage—1.36
D...Single-phase potential applied between all three conductors and ground with them tied together and grounded at other end	Current—1.4
E...Single-phase potential applied between one conductor and ground with it ungrounded at other end	Voltage—1.26
F...Single-phase potential applied between one conductor and ground with it grounded at other end	Current—1.34

line-to-neutral impedance values of 257.5, 257.5, and 258 ohms which were computed using three-phase values taken during test *B*.

Summary of Conclusions

Comparison of measured and computed constants for this type of line construction show:

1. Measured and computed positive sequence units of resistance, reactance, and susceptance are substantially equal. For most practical purposes conventional calculating methods of determining these constants are satisfactory.

2. Measured and computed zero-phase sequence impedances compare favorably. Computed values from line dimensions are satisfactory for most purposes.

3. Measured and computed constants compare favorably for a one-conductor-and-ground channel or circuit of type measured during tests.

4. Greatest difference between measured and computed line constants occurs in zero-phase-sequence resistance and reactance values. This is probably due to assumptions made and difficulty of taking into account all factors in complex physical arrangement of conductor and shield wires and connections of shield wires to ground; resistance and reactance of shield wires varies with current; position of shield wires above conductors varies along line; transpositions; and indefinite value of resistance of earth. Average earth resistivity may be less than assumed 100 ohms per meter cube as shown by values of resistance for zero-phase and one-

conductor-and-ground circuits being less than computed unit values.

5. Ratios of zero-phase-sequence resistance, reactance, and impedance to positive-phase-sequence values obtained from test measurements are less than those obtained from computed values. Ratio of susceptances as determined by measurement are slightly higher than those from computed values.

6. Measured characteristic impedance conductor-to-conductor of 409 ohms is higher than theoretical, but is near 400-ohm value commonly accepted.

7. Measured characteristic impedance of zero-sequence circuit is about 783 ohms and is lower than computed.

8. Measured and computed characteristic impedance conductor-to-ground with shield wires is about 550 ohms depending on current magnitude and ground conditions. One-conductor-and-ground circuit without shield wires may have characteristic impedance much greater than 550 ohms. Thus carrier relay tuning equipment connected between one conductor and ground should have adjustment range sufficient to permit matching impedances considerably above 400- and 500-ohm values commonly used for open-wire transmission circuits.

9. Zero-phase-sequence and one-conductor-and-ground circuit measurements were made with comparatively small currents flowing in shield wires, due principally to inherent low voltage of test method. Although resistance and reactance of shield wires vary with current magnitude and may be a different value during fault conditions when higher currents flow than when measured using low currents, values obtained by measurement check conventional methods of computing line constants.

10. Exact methods should be used in calculations to determine ground and phase currents for relay settings on long transmission lines. Short-line or approximate methods can result in errors of considerable magnitude.

Appendix I. Computation Procedure

Short- and open-circuit impedances viewed from instrument end and listed in Table I were determined from ammeter, voltmeter, and wattmeter readings. Pillar and architrave values of equivalent π network described in Appendix II were computed by means discussed in Appendix III. ***ABCD*** constants of network were determined from these by formulas given in Appendix IV, and were checked using rule $A \times D - B \times C = 1$ as well as other relationships listed in Appendix IV. Unit or per-mile values of resistance, reactance, and susceptance were determined from these ***ABCD*** constants by a cut-and-try procedure discussed in Appendix IV. A second group of ***ABCD*** constants was calculated from these unit values and compared with original ***ABCD*** constants as a check on the per mile values to see that they would yield constants equal to those from which they were derived. This procedure was followed for positive-phase, zero-phase, and one-conductor-and-ground network constants.

Unit positive-phase-sequence values were obtained from manufacturer of conductor and reference 4. Zero-phase-sequence and one-conductor-and-ground-circuit unit values were computed from line dimensions as explained in Appendix V. These unit values are compared with those obtained through measurement as listed in Tables II, III, and IV. ***ABCD*** constants were computed from these theoretical values. These constants also are compared with measured values in same tables, together with open- and short-circuit impedances and characteristic impedances. ***A*** and ***D*** constants were assumed equal.

All calculations were made on conventional 10-inch slide rules with most of them on vector types. No attempt was made to carry calculations and decimal places further than accuracy of basic test data. Considering computation procedure and that exact long-line formulas were used in determining constants and unit values, it is believed results are accurate within plus or minus one per cent.

The 60-cycle impedance of about 0.2 ohm of carrier-current traps was considered too small to be included in the calculations. Since sag and tension add only about one half of one per cent to actual length of conductors over surveyed line length, these factors were not taken into consideration.

While there may appear to be slight repetition of comparison of values, for instance characteristic impedances are given in Tables II, III, and IV and then they are summarized in Table V, this is done to acquaint reader with relative magnitude of these impedances and their relationship.

Positive-phase-sequence ***A*** and ***C*** constants in this paper differ slightly from those given previously [1] because of use of a more direct route of calculating them. That is, knowing ***B*** and Z_{oc}, ***D*** or ***A*** can be determined more directly from $Z_{sc} = B/D$, than from $Z'_{eq} = B/(A-1)$. However, these ***ABCD*** constants do not change unit values previously determined because those values are still within accuracy of method and basic data.

Appendix II. Equivalent Network

Figure 3 represents equivalent π network of transmission line used in calculating constants and checking impedance relationships of positive-phase-sequence, zero-phase-sequence, and one-conductor-and-ground circuit. For positive phase sequence, pillars represent shunt susceptance and architrave equivalent line or transfer impedance. Pillars represent line-to-ground capacitance and architrave the through impedance for all three conductors in parallel which was condition when basic zero-phase-sequence data were taken. For zero-phase-sequence circuit, these equivalent impedances are multiplied by three. For one-conductor-and-ground circuit, pillars represent capacitance between that conductor and ground, and architrave the through impedance.

Referring to Figure 3, Z_{oc} is open-circuit impedance looking into network with opposite end open. Z_{sc} is impedance looking into network with opposite terminals short-circuited together.

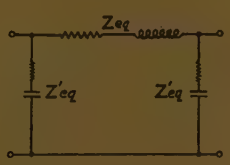


Figure 3. Equivalent π network used to represent positive-phase, zero-phase, and one-conductor-and-ground circuits

Appendix III. Relation Between Z_{eq}, Z'_{eq}, Z_{oc}, and Z_{sc}

Refer to Figure 3:

$$Z_{sc} = \frac{Z_{eq} \times Z'_{eq}}{Z_{eq} + Z'_{eq}} \tag{1}$$

$$Z_{oc} = \frac{Z'_{eq}(Z'_{eq} + Z_{eq})}{2Z'_{eq} + Z_{eq}} \tag{2}$$

From (1):

$$Z_{eq} = \frac{Z_{sc} \times Z'_{eq}}{Z'_{eq} - Z_{sc}} \tag{3}$$

Substituting this in (2) and reducing gives:

$$Z_{oc} = \frac{Z'^{2}_{eq}}{2Z'_{eq} - Z_{sc}} \tag{4}$$

Solving for Z'_{eq}:

$$Z'_{eq} = Z_{oc}\left(\sqrt{1 - \frac{Z_{sc}}{Z_{oc}}} + 1\right) \tag{5}$$

which gives Z'_{eq} in terms of measured Z_{sc} and Z_{oc} values. Substituting results from equation 5 in equation 3 gives Z_{eq}. Equations 3 and 5 were used to obtain components of equivalent π from Z_{oc} and Z_{sc} values as measured.

Appendix IV. Relationships of General Constants and Unit Values

Following formulas were used to obtain ***ABCD*** values from open- and short-circuit impedances, and to check results:

$$Z_{oc} = \frac{A}{C} = \frac{D}{C} \tag{6}$$

$$Z_{sc} = \frac{B}{D} = \frac{B}{A} \tag{7}$$

$$Z_{eq} = B \tag{8}$$

$$Z'_{eq} = \frac{B}{A-1} \tag{9}$$

$$A \times D - B \times C = 1 \tag{10}$$

Expressions for ***ABCD*** constants from references 4 and 5 are:

$$A = D = \cosh \sqrt{ZY} = \left(1 + \frac{ZY}{2} + \frac{Z^2Y^2}{24} + \frac{Z^3Y^3}{720} + \ldots\right) \tag{11}$$

$$B=\sqrt{\frac{Z}{Y}}\sinh\sqrt{ZY}$$

$$=Z\left(1+\frac{ZY}{6}+\frac{Z^2Y^2}{120}+\frac{Z^3Y^3}{5{,}040}+\ldots\right) \quad (12)$$

$$C=\sqrt{\frac{Y}{Z}}\sinh\sqrt{ZY}$$

$$=Y\left(1+\frac{ZY}{6}+\frac{Z^2Y^2}{120}+\frac{Z^3Y^3}{5{,}040}+\ldots\right) \quad (13)$$

Unit resistance, reactance, and susceptance were obtained from equations 11 and 12 and from

$$Z=L(r+jx) \quad (14)$$

$$Y=L(g+jb) \quad (15)$$

by first equating value of A obtained from Z_{oc} and Z_{sc} to expression 11. ZY in series of (11) was found by cutting-and-trying. This was then substituted in (12), and knowing B, Z was found and then Y. Unit values were found from equations 14 and 15. Y was found to have a very small conductance component g, but this was not retained because its value was considered to be too indefinite for methods used.

Appendix V. Unit Resistance, Inductive Reactance, and Capacitive Susceptance From Line Dimensions

Positive-phase-sequence unit constants were obtained from tables and following methods given in chapter III of reference 4. Values obtained are:

$r_p=0.238$ ohm per mile at 25 degrees centigrade and 60 cycles
$jx_p=j0.803$ ohm per mile
$jb_p=j5.18$ micromhos per mile

These do not include effect of earth and shield wires.

Zero-phase-sequence unit constants were obtained following methods given in chapters VII, VIII, and IX of reference 5 and chapters XI and XII of reference 6. Values obtained are:

$r_0=0.829$ ohm per mile at 25 degrees centigrade and 60 cycles
$jx_0=j2.28$ ohms per mile
$jb_0=j3.65$ micromhos per mile

These include effect of shield wires. Average spacings and geometric-mean-distances were used in an effort to include effect of transpositions and different sag of conductors and shield wires.

Unit constants of one-conductor-and-ground circuit were obtained following methods given in chapters VII, VIII, and IX of reference 5 and chapters XI and XII of reference 6. Values obtained are:

$r_1=0.438$ ohm per mile at 25 degrees centigrade and 60 cycles
$jx_1=j1.3$ ohms per mile
$jb_1=j4.6$ micromhos per mile

These values include effect of shield wires. An average geometric-mean-distance between one conductor and shield wires was used in calculations for unit inductive reactance. Likewise average spacings were used in calculations for unit capacitive susceptance.

Following is a summary of basic assumptions, geometric-mean-radii, geometric-mean-distances, and related data used in computing unit constants from line dimensions:

An earth resistivity of 100 ohms per meter cube was assumed.
Equivalent depth of earth return was taken as 2,800 feet.

Conductor diameter = 0.683 inch.
Conductor resistance = 0.238 ohm per mile at 25 degrees centigrade.
Equivalent conductor spacing = 18.25 feet.
Geometric mean radius of conductor for inductive reactance = 0.0245 foot.
Geometric mean radius of conductor group for inductive reactance = 2.02 feet.
Geometric mean radius of conductor group with 14.5 feet spacing for capacitive susceptance = 2.11 feet.

Shield wire diameter taken as 0.375 inch.
Shield wire resistance:
For current of 1 or 2 amperes = 4 ohms per mile.
For 40 amperes = 7.4 ohms per mile.
Shield wire geometric mean radius for inductive reactance:
Low current of 1 or 2 amperes = 10^{-3} feet.
For 40 amperes = 10^{-10} feet.
Geometric mean radius of shield-wire group for inductive reactance:
Low current of 1 or 2 amperes = 0.132 foot.
For 40 amperes = 4.18×10^{-5} feet.

Geometric mean distance of separation between conductors and shield wires = 19.55 feet.
Geometric mean radius of shield wire group for capacitive susceptance = 0.523 foot.
Geometric mean distance of separation between conductors and images for capacitive susceptance = 67 feet.
Geometric mean distance of separation between one shield wire and conductor images for capacitive susceptance = 81.5 feet.
Average geometric mean distance of separation between one conductor and shield wires for inductive reactance = 19.5 feet.

See Figure 2 for average conductor and shield-wire spacings and distance above ground.

Appendix VI. Nomenclature

$ABCD$—General constants of any four terminal network not having an internal source of voltage.
$A_pB_pC_pD_p$—Positive-phase sequence general constants.
$A_0B_0C_0D_0$—Zero-phase sequence general constants.
$A_1B_1C_1D_1$—General constants of one-conductor-and-ground circuit.
$r_pjx_pjb_p$—Positive-phase sequence per mile values of resistance and inductive reactance in ohms and capacitive susceptance in micromhos.
$r_0jx_0jb_0$—Zero-phase squence per mile values of resistance and inductive reactance in ohms and capacitive susceptance in micromhos.
$r_1jx_1jb_1$—One-conductor-and-ground per mile values of resistance and inductive reactance in ohms and capacitive susceptance in micromhos.
Z_{ocp}, Z_{scp}—Positive-phase-sequence impedance in ohms looking into line with far end open-circuited and short-circuited, respectively.
Z_{oc0}, Z_{sc0}—Zero-phase sequence impedance in ohms looking into line with far end open-circuited and short-circuited to ground respectively. These are three times impedances obtained with all three conductors paralleled.
Z_{oc1}, Z_{sc1}—Impedance looking into one-conductor-and-ground circuit with far end open-circuited and short-circuited to ground, respectively.
Z_{eq}—B constant or architrave of equivalent general π network in ohms.
Z'_{eq}—Impedance of shunt path in ohms at two ends or pillars of π network.
Z/θ—Impedance in ohms.
z/θ—Impedance, ohms per mile.
$Z_pZ_0Z_1$—Total line impedance or $L(r_p+jx_p)$, $L(r_0+jx_0)$, $L(r_1+jx_1)$ respectively.
$Y_pY_0Y_1$—Total line admittance or $L(g_p+jb_p)$, $L(g_0+jb_0)$, $L(g_1+jb_1)$ respectively.
L—Line length in miles.

References

1. Staged-Fault Tests of Relaying and Stability on Kansas-Nebraska 270-Mile 154-Kv Interconnection, C. W. Minard, R. B. Gow, W. A. Wolfe, and E. A. Swanson. AIEE Transactions, volume 62, 1943, pages 358–67; discussion, pages 425–6.

2. Performance Requirements for Relays on Unusually Long Transmission Lines, F. C. Poage, C. A. Streifus, D. M. MacGregor, and E. E. George. AIEE Transactions, volume 62, 1943, June section, pages 275–83; discussion, pages 422–5.

3. Protective Relaying on Long Transmission Lines, A. R. van C. Warrington. AIEE Transactions, volume 62, 1943, June section pages 261–8; discussion, page 427.

4. Electrical Transmission and Distribution Reference Book (first edition), Westinghouse Electric and Manufacturing Company, 1942.

5. Symmetrical Components, C. F. Wagner and R. D. Evans. McGraw-Hill Book Company, Inc., New York, N. Y., 1937.

6. Circuit Analysis of A-C Power Systems, Edith Clarke. John Wiley and Sons, 1943.

7. Zero-Sequence Impedances of Overhead Three-Phase and Single-Phase Transmission Circuits, Edith Clarke and G. K. Carter. *General Electric Review*, volume 44, June 1941, pages 329–36.

8. Calculation of Unbalanced Voltage Drops in Distribution Circuits With Particular Reference to Multigrounded Neutrals, W. R. Bullard, H. L. Lowe, and H. W. Wahlquist. AIEE Transactions, volume 63, 1944, April section, pages 145–8.

9. The Electrostatic Unbalance of Transmission Lines and Its Effect on the Application of Petersen Coils, J. A. M. Lyon. AIEE Transactions, volume 58, 1939, March section, pages 107–12; discussion, page 574.

A Pneumatic Mechanism for Outdoor Oil Circuit Breakers

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OPERATING mechanisms for power circuit breakers differ from most machines in that:

1. Moderate amounts of energy must be released quickly and easily at a high power rate.

2. Energy must be available for their operation under emergency conditions, particularly when normal power sources would be interrupted.

These particular conditions are satisfied readily by a stored-energy system where energy is accumulated with moderate rates of power and held ready for instant consumption. Stored-energy systems can be selected in many forms. The rubber bands of a toy airplane are one form, the rocket projectal another. Switchgear however places a premium on reliability, not on lightness or novelty, a demand that considerably restricts the practical choices that can be made.

Of the several choices that are available, storage batteries, compressed springs, and compressed-air systems have histories of satisfactory applications as energy accumulators for power circuit breakers. The high closing speeds of modern oil circuit breakers demand a rather large amount of power during a short interval of time. The larger circuit breakers may require more than 5,000 foot-pounds in 20 cycles during closing. To close a breaker, the mechanism must transform 20 kw of power to mechanical energy. Such demands for power can be supplied most efficiently from an energy accumulator located close to the circuit breaker. Losses over any distance naturally will reduce the available power at the breaker unless the capacity of the transmission channel, such as control-power leads or air lines, is increased considerably. Storage batteries suffer that particular handicap. They are economical when located centrally, and where they can be properly maintained and protected. They are not economical nor very practical when placed adjacent to each of a scattered group of circuit breakers. With batteries located at a central point, the *IR* drop of the control leads is of concern when control power is distributed over the distances to the breakers of a high-voltage yard.

Energy, however, can be economically and practically stored at each circuit breaker as compressed air or as stressed springs. Air can be compressed by small commercial motor-driven air compressors, stored in a moderate-size tank and released by an electrically operated control valve. Energy also can be accumulated and stored by compressing springs through a motor and gear-reduction unit, released and controlled by a suitable system of levers and latches. The methods of accumulating, storing, releasing, and controlling the energy of these media determine the success of the final circuit breaker under operating conditions. The mechanism discussed in this paper uses compressed air as the stored-energy operator. The choice has been made after a careful analysis of the listed advantages:

1. Energy is accumulated through a simple and familiar fractional-horsepower motor and standard air compressor. Power demands are held to a low value.

2. The energy is stored in an air-storage tank at the circuit breaker.

3. The closing power is released and controlled through a low-energy electrically operated air valve, other mechanical reductions being unnecessary.

4. Compressed air has little inertia. It is fast, and its resilience moderates accelerating forces.

5. The speed of response and lack of inertia of compressed air permit a ready reversal of flow to trip free the contacts of a circuit breaker.

Against these choices must be considered the apparent handicaps of compressed-air systems:

1. Storage must be nearly airtight, and, theoretically, leakage must never equal the rate of accumulation by the compressor. Practically, the leakage should be held to a small percentage of this rate.

2. Moisture in the air may give difficulties at freezing temperatures.

The mechanism here described has been developed with emphasis on the elimination or control of the handicaps listed. Practical solutions to the problems of moisture formation with freezing and air leakage have been developed and tested carefully. It is believed that this compressed-air system offers the advantages of a stored-energy system not readily available in other conventional operating mechanisms. See Figure 1.

Operating Characteristics

The value of a circuit-breaker operating mechanism cannot be determined on the basis of energy storage alone, for the choice of an operating medium must include a study of operating characteristics under "closing," "tripping," "tripped free," and "immediate reclosing" performance cycles. This comparison of compressed air with other media already has been presented on the basis of energy storage. The benefits of pneumatic systems when designed to satisfy these other operating characteristics will be discussed.

Closing Characteristics

With ample energy stored at the breaker as compressed air, the breaker contacts can be closed with desirable velocity and force characteristics through an air cylinder. Air compressed above 100 pounds per square inch responds with unusual speed to control demands. Opening the air-control valve admits air to the cylinder assembly at a rate that permits full pressure build up before the contacts are in motion. Pressure is maintained at a rate determined by a controlled orifice, accelerating forces, and the opposing forces of the contacts. Figure 2A illustrates a typical pressure and travel curve as this mechanism closes a standard oil circuit breaker. Contact speeds may be controlled closely by selection of the operating pressure. A controlled flow of air limits the "runaway" and "two-stage" action on contacts characteristic of some prime movers. This two-stage action, or hesitancy when the contacts engage, is particularly undesirable when the breaker is closing against loaded lines or against fault current. Compressed air is positive and powerful in closing a circuit breaker.

Tripping Characteristics

When used with a modern tank-type oil circuit breaker, a pneumatic mechanism uses compressed air to complete only the closing operation. Tripping or opening the breaker is accomplished by simply disengaging a holding latch or prop. The release of this trip latch frees the opening and contact springs to open the contacts. The inertia of most prime movers may retard this opening speed unless the prime

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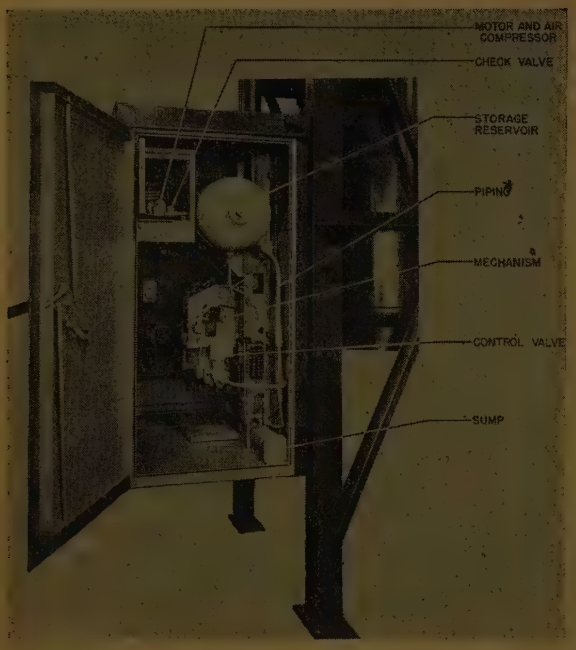


Figure 1. A pneumatic operating mechanism for use with frame-mounted outdoor oil circuit breakers, viewedthrough the right-hand doorway

mover is divorced from the contact structure by a collapsible trip linkage. Such a linkage may delay a reclosure while it resets. The compressed-air mechanism, with little inertia, does not require a collapsible trip linkage. The output crank, freed from the holding latch by the trip coil, swings to its open position, and the mechanism is prepared to reclose the circuit breaker without delay.

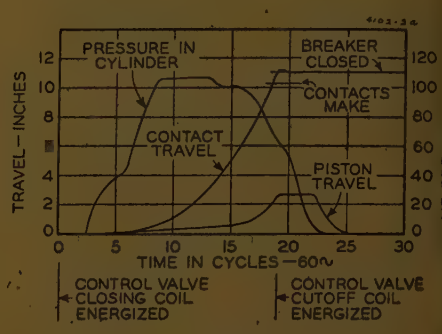
Trip-Free Characteristics

In trip-free operations it is desirable that the contacts of the breaker be reversed in their motion before a closing operation is completed. Heavy solenoids or motors, driving a closing mechanism, would be too slow in their mechanical response to a demand for reversal of motion were it not for the mechanically trip-free mechanisms employed. Such designs use collapsible linkages held rigid by a latch. Placed between the prime mover and the contact structure, this linkage can be collapsed by releasing the latch with a solenoid trip coil. The contacts of the circuit breaker, deprived of their closing force, quickly return to the open position. Unfortunately, the prime mover, being slow in mechanical response, commonly continues to drive onward, acceleration and impact forces replacing the suddenly lost contact-closing load.

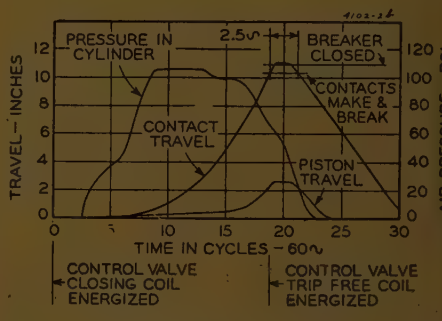
The speed of compressed air and its immediate response to control provides a practical prime mover requiring only speedy control to reverse or release its driving power within the time limits required of a trip-free operation. Fast control is provided by an electrically operated air-control valve. Power release is controlled by a dump or release valve placed in the air line between the control valve and the cylinder. This dump valve is closed or opened by the balance of pressure between the control valve and cylinder. It is closed when the control valve is open, the piston then closing the circuit breaker. If, during the closing cycle the control valve is closed, the sudden loss of pressure on the control valve side of the release or dump valve moves it to the open position, opening the cylinder port and exhausting the air in the main cylinder. With this sudden loss of pressure, and closing force, the circuit-breaker contacts return to their open position (Figure 2B). Thus a trip-free operation is affected by closing the solenoid control valve during the closing stroke of the circuit breaker. Actual operating speeds equal or may exceed those of mechanically trip-free mechanisms using collapsible linkages. Accelerating forces are not sacrificed to moving such links and levers. Excess energy is released as a blast of air from the release valve. An original design of the solenoid control valve provides the speed desirable in the electrical and pneumatic control (see Figure 4).

Immediate-Reclosing Characteristics

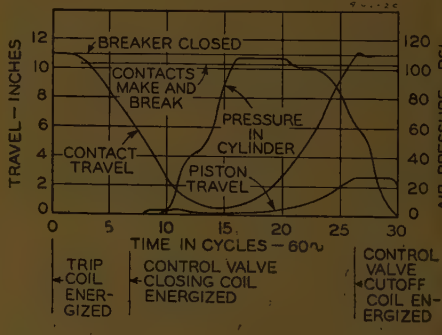
By reclosing instantaneously against a temporarily faulted line, modern oil circuit breakers have, in many applications, made a major contribution to service continuity. In many cases the worth of this contribution is dependent on the speed of reclosure. From the closed and latched position, the system may demand that the circuit be cleared and re-energized in as short a time as 20 cycles. Added to this requirement is the demand that the mechanism be prepared to trip free during the second reclosure. Mechanically trip-free mechanisms are available to satisfy these requirements. Their arrangements are generally particularly designed for such service. On the other hand the standard pneumatically trip-free mechanisms serve this purpose. The pneumatically trip-free mechanism is prepared to open, reclose, trip free, and reclose the contacts of a circuit breaker without delay in succes-



A. A closing operation with 120 pounds per square inch in the storage reservoir



B. A trip-free closing operation at 120 pounds per square inch, with the trip circuit energized through the main contacts for this no-load test. The short time in contact (2.5 cycles) demonstrates the speedy response of this pneumatic operator



C. An instantaneous-reclosing operation at 120 pounds per square inch. Attention is directed to the initial travel of the piston at low pressure. Here it meets the backward movement of the contacts, gradually stopping their motion and returning them to the closed position

Figure 2. Time-travel curves of a pneumatically operated standard outdoor oil circuit breaker

Instantaneous relations of cylinder pressure, piston travel, and breaker-contact travel are recorded. The curves are to a common scale

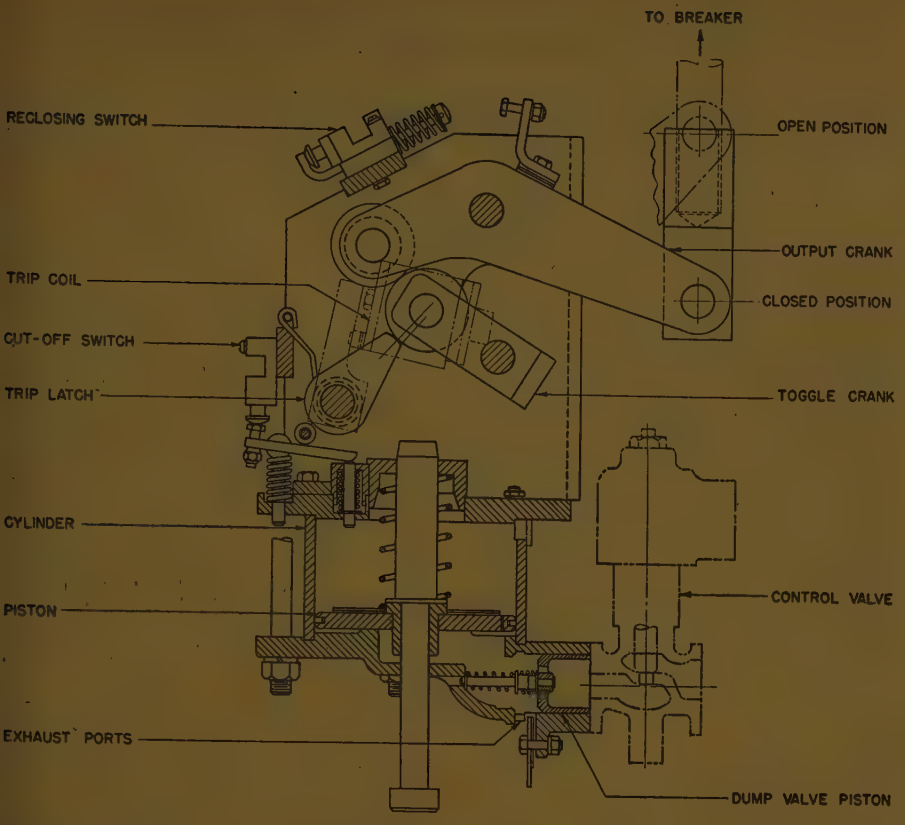


Figure 3. A diagram of a pneumatically trip-free operating mechanism latched in the closed position

The control valve has closed; pressure in the cylinder has moved the dump-valve piston to the right, uncovering the ports and exhausting the cylinder

sive operations (Figure 2C). Proper attention to control circuits makes this a most satisfactory operating mechanism for instantaneous-reclosing applications.

Description of the Mechanism

Functionally the operating mechanism may be divided into:

(*a*). The prime mover, mechanical linkage, and operating controls.

(*b*). Compressor, air-storage equipment, and controls.

The Prime Mover, Mechanical Linkage, and Controls

The arrangement of the mechanism is illustrated in Figure 1. It includes a frame, a closing linkage, a trip latch, a cylinder and piston assembly, a dump valve, and a control valve. The mechanism is supported rigidly on a pair of vertical channels forming part of the housing and bolted directly to the supporting framework of the oil circuit breaker.

The closing linkage, Figure 3, consists of the output and toggle cranks arranged to operate as rolling toggles during the closing and in the latched position. As a toggle this system adjusts the closing forces of the piston to the contacting forces of the circuit breaker. In the closed position a trip latch or prop drops under a roller on the lower toggle crank. Until this latch is forced from under this roller either by the trip solenoid or by hand, the breaker contacts are locked closed. An air-operated cylinder and piston are mounted directly to the mechanism frame. The piston plunger operates against the roller of the lower toggle crank to force the mechanism to the closed and latched position. A hydraulic jack may be used against the lower piston extension for manual operations when the breaker is being inspected or maintained. Because air pressure in the cylinder is transient, it is not necessary to seal this part of the system against normal leakage. The piston is fitted with a conventional piston ring. Mechanical clearances are not unduly close.

The operation of the air piston is controlled by an electrically operated air valve shown in Figure 4. Energizing the "operating coil" at the top of the valve lifts an armature coupled to a pilot valve. As this valve is opened, an unbalance of pressures opens the main piston valve. In the open position the armature seals against a small permanent-magnet pole piece. Near the end of the power piston's closing stroke an auxiliary switch energizes a small cutoff coil adjacent to the magnetic pole piece. The flux of the permanent magnet is shunted out by the field of this coil and releases the armature. The pilot valve and, in turn, the main control valve close. With the control valve

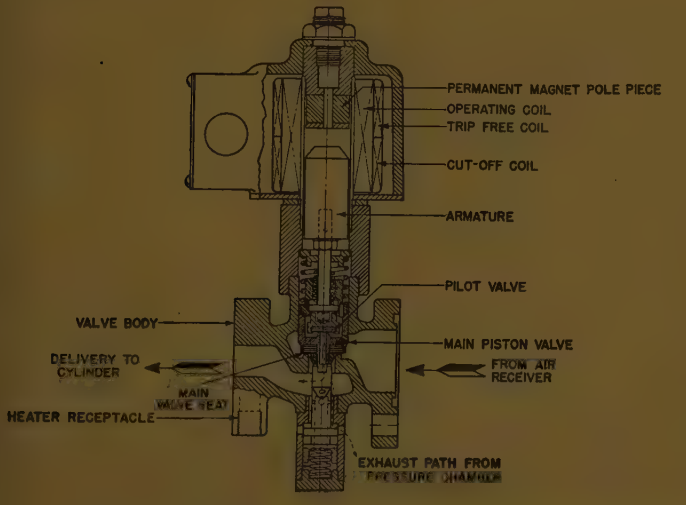


Figure 4. An electrically trip-free air-control valve

The valve is sealed open magnetically and recloses as the holding flux is shifted by energizing either the trip-free coil or the cutoff coil

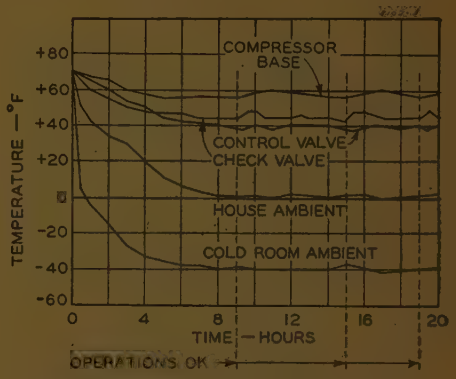


Figure 5. Records of a representative test of a pneumatic mechanism under controlled cold-room conditions

Air was accumulated in the storage reservoir while the room was at 70 degrees Fahrenheit with a relative humidity of 96 per cent. The ambient temperature of the room was then dropped to and held at 40 degrees Fahrenheit below zero. The compressor, pressure switches, control, dump, and check valves were operated at indicated intervals. Recorded temperatures are shown during this test run

closed, the dump valve slides to its open position, and the compressed air in the operating cylinder is released.

For trip-free operations a small trip-free coil similar to the cutoff coil is energized in parallel with the trip coil, the control valve is closed, and again the compressed air is released from the main cylinder. This trip-coil circuit can be energized from a conventional d-c source. The trip-coil currents are comparable to those of other mechanisms, being six amperes at 125 volts direct current.

Compressor, Air-Storage, and Control Equipment

The problems peculiar to compressed-air mechanisms for circuit breakers include reliable air storage and freedom from air stoppages due to the freezing of accumulated moisture. As an answer to these problems, every effort was made to simplify all piping and to have all such piping drain to a common sump. A minimum of joints reduces the possibility of leaks. Proper drainage reduces the difficulties with accumulated moisture.

A standard-type air compressor, driven by a fractional-horsepower motor, is mounted adjacent to the storage reservoir near the top of the mechanism housing. A coil of formed tubing serves as an after-cooler between the compressor and the storage tank. The tank, manufactured to all code requirements, has storage capacity for at least five successive breaker operations. Condensate is accumulated in a small sump at the lowest point in the house. A hand valve is used to blow off this sump.

Pressure switches control the air system. The first switch controls the compressor motor and regulates the pressure at the reservoir. A second pressure switch opens the breaker-closing-control circuit if the storage pressure is below a working minimum. A third switch can be used to operate a minimum pressure alarm. A pressure gauge, safety valve, and check valve complete the air-system accessories.

Experience with compressed-air systems has emphasized the value of careful design and design testing to prevent possible difficulties from freezing. Any troubles at freezing temperatures originate with moisture accumulations. The air lines of the operating mechanism are laid out to reduce moisture pockets to a minimum.

As a guard against freezing during subzero temperatures, small resistance heaters are installed at the control valve and on the supporting base of the compressor. A small space heater, located below the mechanism, moderates the temperature of the house. These heaters are controlled by a thermostat which energizes these heaters when the inside ambient temperature is less than 50 degrees Fahrenheit.

With these provisions, operations have proved satisfactory to 40 degrees below zero Fahrenheit, under the controlled-test conditions of a refrigerated room. During an extended series of tests in this refrigerated room the compressor accumulated air of both low and high moisture content. Continuous tests were recorded over extended periods to duplicate the more severe operating conditions. Figure 5 summarizes some of the more interesting records.

Summary

This compressed-air mechanism has been developed to answer the requirements of a simple and reliable stored-energy mechanism. By arranging this mechanism to be pneumatically trip-free, it has been possible to get the high-speed operations required of modern oil circuit breakers. The mechanism has been tested under severe operating conditions to insure trouble-free performance. Time-travel curves show satisfactory characteristics for closing, tripping, trip-free, and instantaneous reclosing operations. The absence of synchronized mechanical motions and critical mechanical adjustments contributes to a reduction in maintenance and will add to reliable operation.

References

1. A Compressed-Air Operating Mechanism for Oil Circuit Breakers, R. C. Cunningham, A. W. Hill. AIEE Transactions, volume 61, 1942, September section, pages 695–8.

2. New Multibreak Interrupter for Fast-Clearing Circuit Breakers, R. M. Spurck, H. E. Strang. AIEE Transactions, volume 57, 1938, December section, pages 705–10.

3. Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines, D. C. Prince, Philip Sporn. AIEE Transactions, volume 56, 1937, January section, pages 81–90.

Insulation Temperature Standards for Industrial-Control Coils

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FOR many years, American Standard C19 (Underwriters Laboratories, Inc. issue of 1938 and National Electrical Manufacturers' Association industrial-control standards publication 40-59, April 1940) for industrial-control apparatus has specified an allowable temperature rise for industrial-control coils with class-*A* insulation of 85 degrees centigrade by resistance. Although this is 25 degrees centigrade higher than the value of 60 degrees centigrade rise by resistance established for rotating machinery with class-*A* insulation, service experience with control coils has been thoroughly satisfactory. It is estimated from field experience that not more than five per cent of all coil failures have been caused by temperature, despite the frequent use of above-normal voltages. The author believes, therefore, that the present control standard has proved to be conservative with respect to temperature-rise limits.

The chief reasons for the superior temperature–life expectancy of industrial-control coils as compared with rotating machinery having class-*A* insulation are their rigid structure and small size. These conditions together with the thorough varnish sealing possible, eliminate the failures from brittle insulation that occur in large apparatus subjected to vibration.

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Service Factors

This fact that insulation life depends very largely on the type of apparatus and service, as well as upon the insulating materials used, was recognized in the 1940 revision of AIEE Standard 1, as indicated by the following quotations from its preface:

". . . How long an insulation will last electrically will depend not only on the class of material used, but also on the effectiveness of the physical support for the insulation, and the severity of the physical forces tending to disrupt it. Even though portions of insulation structures of organic materials

have become oxidized or embrittled under the influence of high temperatures, successful operation of the windings may be obtained over further periods of years, if such insulation is not mechanically disturbed.

"In view of the important influence of mechanical stresses, thermal expansion and contraction forces impose temperature-rise limitations on larger apparatus, even though higher temperature rises have proven to be satisfactory in smaller machines.

"Insulation life is also dependent to a considerable extent upon the access of oxygen, moisture, dirt, or chemicals to the interior of the insulation structure. At a given temperature, therefore, well-impregnated windings or totally enclosed machines may have much longer insulation life than windings freely exposed to industrial atmospheres. . . .

". . . It is important that individual types of apparatus and specific-purpose applications should be free to use different temperature-rise values in accordance with their individual economic problems."

At any rate, hundreds of thousands of class-*A*-insulated industrial-control coils, designed for a rise by resistance of 85 degrees centigrade at 100 per cent voltage, have been in use in industrial applications for the past 25 years with entire success and customer satisfaction of a high order. This experience, therefore, supports the author's conviction that the temperature-rise limits should be extended:

(*a*). To take advantage of advances made in the art of coil design and manufacture.

(*b*). To utilize to the fullest possible degree opportunities offered by the many new synthetic materials which have come on the market in recent years.

Better Designs and Service

In reference to paragraph *a*, such improvements as have been made in molded compounds, varnishes, cloths, paper, and wire should be and have been reflected in better and longer-lasting coils. Designs today tend to be even smaller and more compact and for this reason do not readily deteriorate mechanically subsequent to overheating (for typical appearance of control-coil designs see Figure 1). Improvements in coil construction will result in smaller and lighter designs. These when used in contactors and relays speed up device operation, prolong life of moving parts, tend to reduce maintenance, and, in general, give greater customer satisfaction.

Also much has been learned about methods of support so that present coil designs are well and firmly held in place. This means that the drying out of the insulation at elevated temperatures will not result in mechanical failure of the insulation system. Another factor which is of considerable help is the use of class-*A* non-water-absorbent insulation materials. Old-time papers and cloths when dried out and later subjected to moisture conditions act as a wick absorbing considerable quantities of water. This action, of course, is productive of current leakage and electrolytic corrosion of the copper wires and is far more destructive to coil life than temperature aging by itself.

This temperature-aging moisture-absorption characteristic does not apply in the same degree to modern synthetic insulation materials.[1] Cellulose acetate, polystyrene, polyvinyl-chlorides, polyamides, and the glass or glass-laminated products are far better than the papers, silks, and cottons in common use when the present standards were first written. This is substantiated by field experience which has shown that temperature failures are less than five per cent of total coil failures.

New Materials

Again if we consider paragraph *b*, these new materials[2] and their applications do not fall too well within our previous concept of class-*A* materials. Nylon, for instance, is a good illustration of a material of organic origin that will withstand temperatures in excess of those permitted by AIEE class *A*. On the other hand it cannot be classified as class *B*. There are many materials of this general nature that will be available after the war.

For instance, as an illustration of the many new synthetic materials which favor a change to higher temperature standards, we have such materials as General Electric Company Permafil, Westinghouse Electric and Manufacturing Company Fosterite, Bakelite Corporation number 16,631, Columbia Corporation resin *CR* 39, and Monsanto Chemical Company number 100 type of resin, and a host of others, manufactured by such companies as Klascon, Ault and Wiborg, Marco, and others. These materials are chemically different from old-style conventional oil varnishes and beyond a doubt, far superior at elevated temperatures when properly applied.

Test Data

Actual long-time test data running into years of field experience are not available for most of these materials, operating at their top temperature limits. However, much practical field experience has been gained over the last two-year period in applications where these materials have been in demand. Such applications as common to the aviation industry are very nicely taken care of by the high power factor, high dielectric and temperature-endurance characteristics of these synthetics.



Figure 1. Typical industrial-control contactor coil

A few selected cases of test data on coils made with General Electric Company Permafil are:

A. A group of coils wound with 1,100 turns of 0.0179 wire were operated 600 hours at 185 degrees centigrade total. Slight cracking of the insulation appeared at one end of one coil, but the others remained in perfect operating condition throughout the test.

B. Another group of coils being slightly larger, approximately four inches square, and consisting of 2,000 turns 0.0126 wire have to date been operated for 850 hours at 165 degrees centigrade total temperature. To date there is no indication whatever of insulation deterioration.

C. Yet another group of coils of the paper-layer-wound variety insulated with Permafil and consisting of 6,000 turns of 0.010 wire have operated 1,000 hours at 150 degrees, centigrade without any signs of cracking, crazing, shrinkage, or other deterioration

D. A filament transformer has been operated on test 1,500 hours, with a voltage between terminals of 25 kv at a total temperature of 145 degrees centigrade. This coil is still in perfect condition.

Laboratory Tests

Much work has been done by the various laboratories interested in improving insulation materials. This, of course, applies to nearly all leading electrical manufacturers. Tests made by the author's associates include heat aging on test specimens at 150 degrees centigrade for one-year period and comparison of power factor and dielectric after this period of time. Among the ma-

terials tested, Permafil showed no deterioration during these tests. This applies as well to the electrical characteristics as to such mechanical considerations as shrinkage, cracking, discoloration, or embrittlement.

From the multitude of tests mentioned, the conclusion is evident that the heat-resistant synthetics of today are much superior to the common class-*A* materials. What shall we do with these new materials? The author believes that now is the time to appraise these newcomers to the insulation field and where available introduce them on current designs. However, to be able to do this, we must change the rules under which we operate.

New Heat-Resisting Synthetic Insulating Materials

It is recognized that all synthetics are not improvements over natural organic insulating materials. For this reason it is very difficult to classify the heat-resistant synthetics or to group them under family names; most of them do not have readily understandable names and are known only as numbers. It is proposed, therefore, that these materials be grouped under the broad title of "Heat-Resisting Synthetic Insulating Materials." The acceptance of any particular material under this definition ultimately should be dependent on proof that it meets the requirements established by a (future) test code of insulating materials, or by accelerated life tests on complete coils.

Application Problems

The existing temperature standards referred to in the introduction are based on continuous operation. This, of course, does not apply to the vast majority of control-coil uses, and for that reason recognition should be given this fact as part of the standards.

Actually, very few control coils or motors operate under continuous-service conditions. This is a point that sometimes is overlooked in considering insulating materials. Much has been written about life expectancy of class-*A* materials.[3] The literature indicates that the useful life of organic insulations such as cotton, oil, and paper, is of the order of 7 to 15 years when exposed to 105 degrees centigrade continuously, with free access of air and moisture, and that the life is halved by each 8 to 12 degrees centigrade increase in temperature. Of course, the actual life in a particular case is increased if oxygen and moisture are excluded,[4] and if exposure to maximum temperature occurs only part of the time. These latter factors account for the fact that control-coil life under present temperature-rise standards has approximated ten years, in the author's experience.

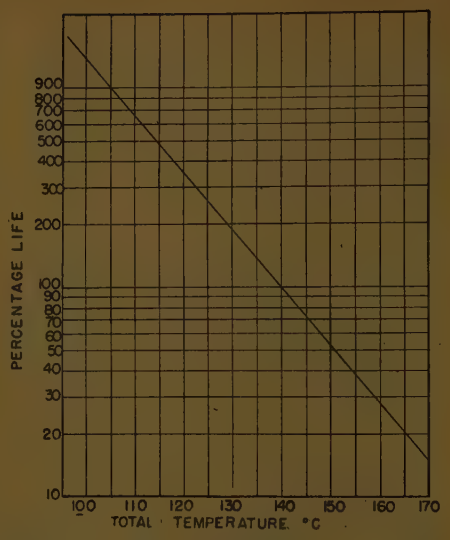


Figure 2. Effect of temperature on life of class-*A* insulated magnet coils

Taking everything into account, the author believes that an 11 degrees centigrade temperature increase for half life is the most reasonable assumption for organic insulation in agreement with reference 3 and as indicated in Figure 2. This curve is drawn with the 100 per cent life point located at 140 degrees centigrade, corresponding to 100 degrees centigrade rise over a 40 degrees centigrade ambient temperature. This curve is useful in predicting life expectancy at higher or lower temperatures of otherwise identical applications.[5]

It is expected that the new temperature-resisting synthetic materials will have an even better service life at this 140 degrees centigrade total temperature value than has been the case with the older class-*A* insulation at the present limiting temperature of 85 degrees centigrade rise plus 40 degrees centigrade ambient temperature, or 125 degrees centigrade total.

It is of extreme importance to judge such data correctly in terms of probable life under actual operating conditions. Duty cycle, ambient temperatures, time for coils to attain maximum temperature, enclosures, and all other special conditions must be considered. It is and should be the application engineers' problem to evaluate such data. This cannot be done by blind adherence to present AIEE Standards.

For instance, in the case of aircraft control coils, these are made to give satisfactory service at temperatures well above the 85 degrees centigrade rise plus 40 degrees centigrade ambient temperature. Considering expected life, this is sound application engineering and the AIEE rules should be made flexible enough to include such cases.

Hot-Spot Temperatures

With today's design practices, the hot-spot differential for industrial-control coils is much lower than in past practice and is normally below the five degrees centigrade contained in AIEE Standard 1.

The elimination of much fabric coil and layer insulation by the substitution of new extra-tough enamels and other dense materials has reduced the temperature gradient to practically zero.

Recommendations

In view of the preceding considerations, it is recommended that the following changes be made in the present AIEE Standard 1 applying to industrial-control coil designs:

1. For industrial-control coils of 600 volts rating or less, using chiefly heat-resisting insulating materials, not dependent for their insulating strength on any natural organic materials such as cotton, paper, or varnished cloth, and thoroughly impregnated with high-temperature synthetic varnishes, the permissible temperature rise by resistance should be 100 degrees centigrade.

2. For industrial-control coils of 600 volts rating or less, with insulation composed of a high percentage of inorganic materials, such as glass or mica, and wound on assemblies of heat-resisting synthetic insulating materials, the permissible temperature rise by resistance should be 120 degrees centigrade.

References

1. For descriptive data on corrosion factors: Corrosion Causes and Remedies in Fine-Wire Magnet Coil, B. W. Erikson. *Product Engineering*, March 1943.

2. For an article on new materials: Electrical Application of New Thermoplastics, T. S. Carswell, Turner Alfrey, Jr. *Electrical World*, June 12, 1943.

3. Temperature-Aging Tests on Class-*A* Insulated Fractional-Horsepower Motor Stators, J. A. Scott, B. H. Thompson. *Electrical World*, June 12, 1943.

4. Chemical Changes Affecting the Stability of Cellulose Insulation, F. M. Clark. Electrochemical Society *Transactions*, 83.81, April 1943.

5. Application of Apparatus and Conductors Under Various Ambient-Temperature Conditions, R. E. Hellmund, P. H. McAuley. AIEE Transactions, volume 61, 1942, August section, pages 553–8. (Shows a national average of 22 degrees centigrade.)

Gas-Filled-Cable Research and Experience

G. B. SHANKLIN
MEMBER AIEE

RESEARCH and engineering studies of gas-filled cable were started about 2 years ago by the company with which he writer is associated and this develop- ient work has been carried on con- inuously since then. Useful results in he form of practical cable designs have esulted and more will follow in the future s our knowledge of the basic principles f this type of cable increases. The resent paper will be in the form of a prog- ess report summarizing results ob- ained since the first two papers[1,2] on his subject were presented before the IEE in 1939 and 1942. The same meth- ds of test and analysis of test data escribed in the previous papers will be ollowed and, although an attempt will e made to have the present paper com- lete in itself, it will be necessary to refer o the older papers frequently to avoid epetition.

The soundness of gas-filled-cable de- gn is now well proved by more than ve years of practical operating experi- nce and this type of cable can no longer e considered experimental. There have een no service failures or signs of insula- on deterioration and during this period total of something like 20 separate in- allations involving low-, medium-, and igh-pressure types, at voltage ratings om 15 to 120 kv, have been produced this country by cable manufacturers. Iost of this experience has been with he low-pressure type at voltage ratings p to 38 kv. There has, however, been nited experience with the medium- and igh-pressure types. Uniformity and ontrol of dielectric characteristics through gas-pressure control, simplicity of design and operation, economy, self-supervision of mechanical defects, and other features assure future usefulness.

Ionization as a Function of Gas Pressure

The establishment of a reliable curve showing initial ionization starting voltage as a function of gas pressure furnishes a useful and important guide for design and operation of gas-filled cable. A curve of this kind was given in the last paper[2] and its significance was discussed at some length. Due to the ionization self-extinguishing effect in mass-impregnated gas-filled cable, which will later be dealt with in detail, the initial ionization starting voltage represents an operating voltage limit that can be closely approached with safety. In this respect, gas-filled cable is the only known type that offers a direct way of predetermining safe operating voltage stress and insulation thickness by means of a simple, short laboratory test on advance samples. It is for these reasons that close attention has been paid to the establishment of a reliable average curve of this type.

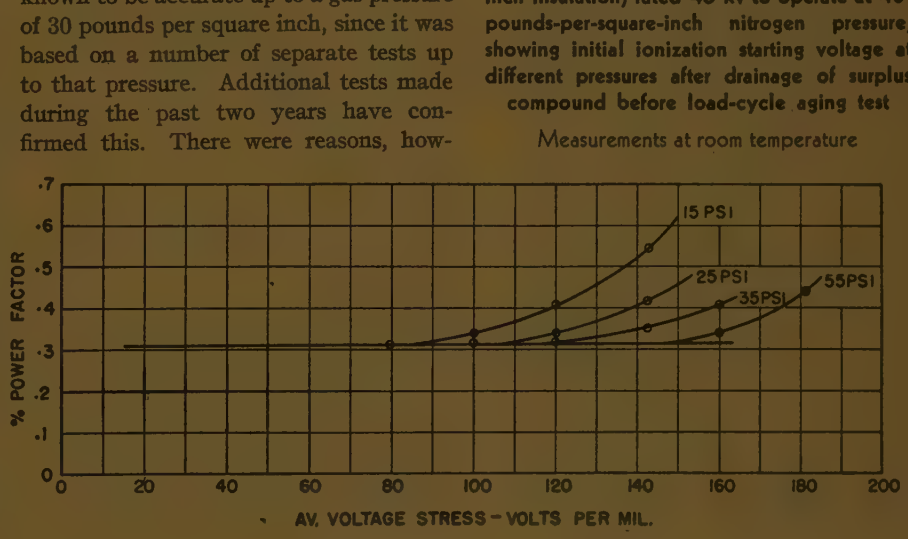
The curve in the earlier paper[2] was known to be accurate up to a gas pressure of 30 pounds per square inch, since it was based on a number of separate tests up to that pressure. Additional tests made during the past two years have confirmed this. There were reasons, however, to doubt the old curve at pressures above 30 pounds per square inch. For one thing, the shape of the curve at the higher pressures did not agree with theoretical calculations, having too much droop. The peculiar shape of the power factor curves in Figures 3 and 4 of the first paper[1] also indicated the possibility of extraneous effects. Later, it was confirmed that the gas-filled test terminals were faulty and terminal discharges accounted for these discrepancies. Additional high-pressure measurements have since been made under better controlled conditions.

Figure 1 gives power-factor curves at pressures from 15 to 55 pounds per square inch on a commercial length of 46-kv three-conductor gas-filled cable with double-reinforced sheath. The cable was impregnated with standard number 5317 compound and had received the usual standard treatment and compound drainage before test. It will be noted that ionization voltage up to 30 pounds per square inch agrees with that measured on standard low-pressure gas-filled cable as reported in the past[1,2] the ionization voltage stress being 85 volts per mil at 15 pounds per square inch.

Figure 2 shows corresponding power-factor curves at pressures from 10 to 225 pounds per square inch on a special length of hollow-core single-conductor high-pressure cable in a steel pipe. It

Figure 1. Three-conductor medium-pressure gas-filled cable, 400,000 circular mils, compact round conductors, strand shielding, 0.280-inch insulation, rated 46 kv to operate at 40-pounds-per-square-inch nitrogen pressure, showing initial ionization starting voltage at different pressures after drainage of surplus compound before load-cycle aging test

Measurements at room temperature



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he author acknowledges the co-operation of J. B. lter, who carried out the laboratory tests, V. A. eals, L. Wetherill, C. A. Piercy, L. L. Phillips, d A. R. Lee.

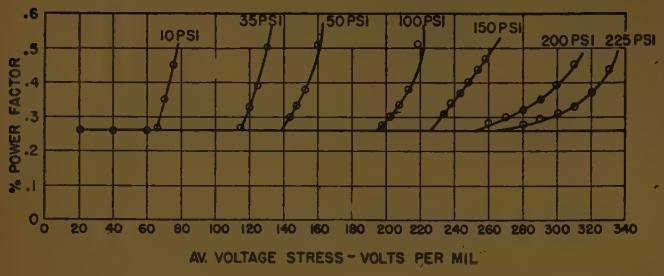


Figure 2. Single-conductor high-pressure gas-filled cable—350,000-circular-mil conductor with one-half-inch hollow core, strand shielding, 0.300-inch insulation, lead sheath, cable in steel pipe, showing initial ionization starting voltage at different pressures after very severe drainage of insulation at 100 degrees centigrade

Measurements made at room temperature

was also impregnated with standard number 5317 compound in the usual way but the compound drainage treatment was much more severe than standard, the purpose being to remove all possible surplus compound from the insulation and thereby obtain more sharply defined curves. It is always difficult to obtain sharply defined ionization starting voltage with standard cable, especially at the higher pressures. Obviously, also, such results obtained on severely drained cable are on the safe side when applied to standard cable.

In this case, drainage treatment before leading consisted of application of ten pounds per square inch nitrogen pressure to the conductor core, with the reel length under vacuum in an impregnating tank at 100 degrees centigrade. This forced drainage continued for two days and examination of end samples showed the whole cable cross section to be quite "dry," with little evidence of surplus compound between tapes. This "dry" condition is further verified by the sharply defined ionization curves in Figure 2, and the relatively low ionization starting point of 65 volts per mil at ten pounds per square inch. Standard cable averages 75 volts per mil at this pressure.

The results in Figures 1 and 2 were replotted in Figure 3 and a revised curve of initial ionization starting voltage as a function of gas pressure constructed as shown. Up to 30 pounds per square inch this curve is the same as the old one.[2] Above 30 pounds per square inch the new curve has a much more rational shape and agrees better with the theoretically calculated curve. That there is not closer agreement might be due to any one of several things. Theoretical calculations of this kind are, at best, approximate. Extraneous results at the higher pressures and stresses might possibly still be present in the test measurements, even though of lesser degree. Finally, the "dry" condition of the insulation undoubtedly lowered the measured curve below the theoretical curve at the higher pressures, since the latter was based on results with standard cable up to 30 pounds per square inch, the extension above this pressure being calculated from Paschen's law,[3] corrected for measured dielectric strength of nitrogen gas at the higher pressures.[4]

Working Voltage Stresses

The disagreement between calculated and measured results is not important, since even the measured curve in Figure 3 shows ionization voltage stresses at the higher pressures to be well above any working stresses that might be used in actual designs. For purposes of co-ordinated impulse strength the insulation thickness for a 138-kv high-pressure cable would correspond to an average 60-cycle working voltage stress of not more than 145 volts per mil. Figure 3 shows that even with a gas pressure as low as 80 pounds per square inch this working voltage stress is well below ionization voltage stress. At present, reinforced-sheath medium-pressure gas-filled cable operates at a pressure of 40 pounds per square

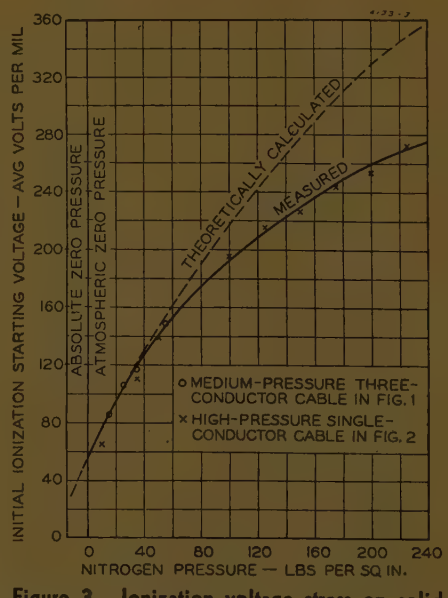


Figure 3. Ionization voltage stress on solid insulation at which initial ionization starts in mass-impregnated gas-filled cable after drainage of surplus compound but before endurance test voltage is applied

inch and an average working voltage stress of 100 volts per mil. Reference to Figure 3 will show this to be well below the ionization-voltage curve. To a lesser degree, the same thing can be said of single-sheath low-pressure cable operating at 12 pounds per square inch and 65 volts per mil. In all three types there is room for further reduction in insulation thicknesses in the future, as far as 60-cycle voltage stresses are concerned. In this respect, no attempt has yet been made to take full advantage of the ionization self-extinguishing characteristics of mass-impregnated gas-filled cable.

Automatic Grading and Self-Healing

Some engineers have questioned the fact that mass-impregnated cable is designed on the basis of average voltage stress rather than maximum stress and that comparative long-time load-cycle tests, as previously reported,[2] have shown no appreciable advantages for strand shielding or extra thin paper tape in the high-stress zone. These things appear to contradict theory, which tells us that the high-stress zone near the conductor is critical and needs to be strengthened by such expedients as strand shielding, thin paper tape, and limitations of maximum stress. These things have not proved necessary because of the pronounced ionization self-extinguishing characteristics of this type of cable, which is something that theory does not take into account.

This action may not yet be fully accounted for but recent studies have given a better understanding of it, as will be described. From the beginning, it has been understood that wax formation in the void spaces has had a good deal to do with extinguishing ionization discharge in its incipient stage and before it has had a chance to grow and become cumulative. The puzzling thing has been where all of the surplus compound came from that formed this wax, which completely or partially filled up the void spaces. This was not made clear until lengths of gas-filled cable impregnated with compounds covering a wide range of viscosities were subjected to long-time load-cycle tests and their behavior closely observed. It is now known that if the impregnating compound is of sufficiently low viscosity to be mobile it will be drawn into the high-stress field both by capillary action and by the electric stress field, itself. The voltage stress will also draw this compound into the low-specific-inductive-capacity voids and thus automatically rearrange the distribution of

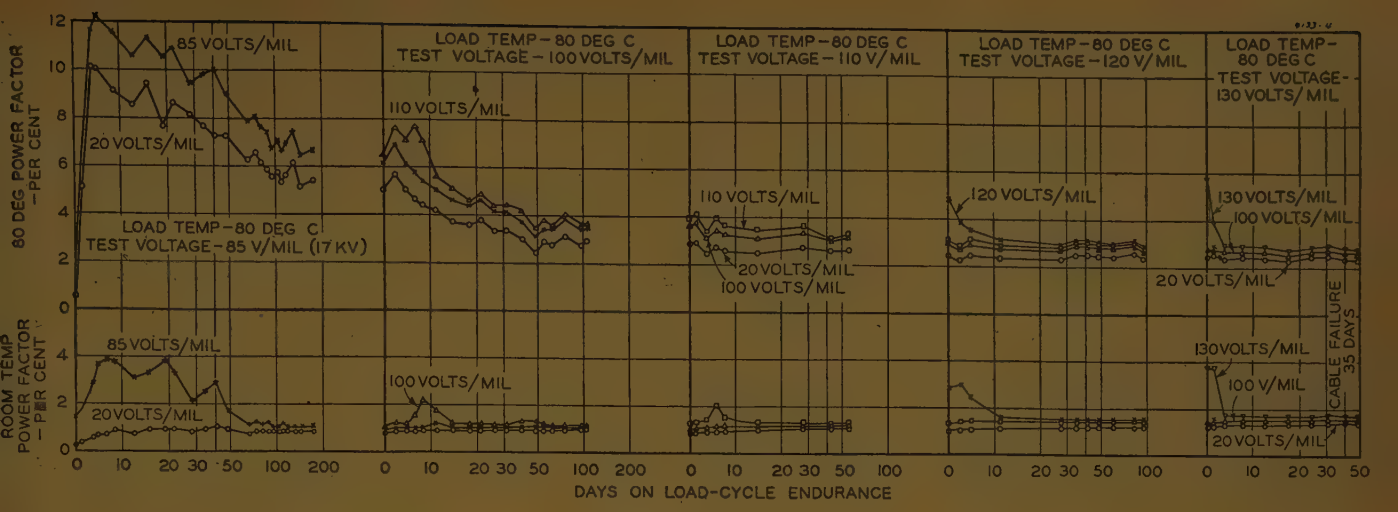


Figure 4. Load-cycle endurance at ten-pounds-per-square-inch nitrogen gas pressure

Three-conductor gas-filled cable of standard construction, with exception that impregnating compound was thin number 5,314 oil having a Saybolt viscosity of 37 at 100 degrees centigrade, insulation thickness 0.200 inch

compound in the cable cross section to those locations where it is most needed. For convenience, this is termed automatic-grading action and the formation of wax, tending to fill up the voids, is termed self-healing action.

Both work together in preventing cumulative ionization in the high-stress zone and, in fact, throughout the whole cable cross section. Dielectric uniformity with absence of "weak spots" has always been an ultimate goal in high voltage cable practice. It is obtained in oil-filled cable by maintenance of oil pressure which, in turn, assures complete absence of voids.[5] In mass-impregnated gas-filled cable it is obtained by controlled gas pressure and the use of a mobile impregnating compound of optimum viscosity. Voids do form but their good behavior is assured by these automatic-grading and self-healing actions.

Load-Cycle Endurance Tests

Cable Impregnated With Thin Oil

These actions were first made clear by the surprising results obtained on load-cycle endurance test with a length of three-conductor cable impregnated with relatively thin number 5,314 oil, such as used in oil-filled cable and having a Saybolt viscosity of 37 at 100 degrees centigrade. The results are charted in Figure 4, the methods of test duplicating in all details those described in the two previous papers.[1,2] The description of test will not be repeated here, except to state that the cable was of the low-pressure shielded type with 350,000-circular-mil unshielded compact sector conductors, 0.200-inch insulation, and single lead sheath.

As would be expected, there was very pronounced drainage of this thin oil from the cable structure before leading, the outer surface having a dry appearance, with only that oil held in the denser parts of the cable cross section by capillary attraction being present. The extreme drainage and relatively large size of voids was verified by the initial power factor measurements at ten pounds per square inch, the initial ionization starting voltage stress being only 48 volts per mil, which is much below average standard results as given in Figure 3.

Figure 4 shows that during the first ten days of the first endurance step at 85 volts per mil there was pronounced ionization and instability. It was so marked that early failure of the cable length was expected. From this time on, however, the cable gradually improved. At the end of 125 days ionization had disappeared, due to closing up of the critical voids. When stability was assured after 189 days the life-test voltage was increased to 100 volts per mil. Ionization and instability were much less marked and disappeared after 95 days, the next voltage step being started after 103 days. These voltage steps were continued with the same results until final failure occurred after 48 days at 130 volts per mil.

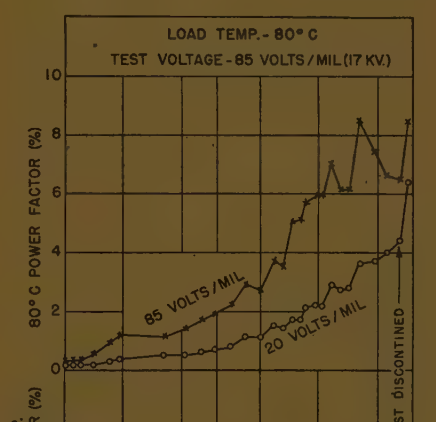


Figure 5. Load-cycle endurance at ten-pounds-per-square-inch nitrogen gas pressure

Three-conductor gas-filled cable of standard construction, with exception that impregnating compound was a heavy hydrogenated oil having a Saybolt viscosity of 1,000 at 100 degrees centigrade, insulation thickness 0.200 inch

The important point to note is that this cable showed even more self-healing ability than standard cable impregnated with heavier oil. Its endurance with final failure at 130 volts per mil, was also as good as the best results obtained with standard cable on these long time tests.

Dissection of the cable after test showed no evidence of the original thin oil. The paper tapes were quite dry and the butt spaces were completely filled with a gummy oil residue, representing that stage of polymerization before final *X* wax is formed. This residue was less gummy and more nearly resembled *X* wax as the conductor was approached. The strand spaces at the unshielded conductor were completely filled with a residue that was practically in the form of final *X* wax. The evidence was quite plain that most of the thin oil left in the paper tapes had been drawn into the low-specific-inductive-capacity voids by electric-field forces and had also been drawn towards the conductor, the paper tapes in the inner zone being less dry than those in the outer zone.

Cable Impregnated With Extra-Heavy Compound

Just the opposite results were obtained with a duplicate length of cable impreg-

nated with a very heavy hydrogenated oil having a Saybolt viscosity of 1,000 at 100 degrees centigrade. The load-cycle test on this cable is charted in Figure 5. Initial ionization voltage after drainage and leading was 75 volts per mil at ten pounds per square inch, which is a normal average value for standard cable and shows good initial impregnation and good retention of compound during drainage. In spite of this good start, however, Figure 5 shows marked instability after the first three days of load-cycle testing at 85 volts per mil. Dielectric losses and ionization discharge increased steadily and there were no signs of stability. Test was, accordingly, continued at 85 volts per mil for 277 days and since there were still no signs of ionization being extinguished, and stability reached, the test was stopped without attempting to go to a higher voltage step.

Dissection of the cable after test showed dry *X* wax deposits in the butt spaces throughout the cross section of insulation. These deposits became more pronounced at the inner layers of paper and at the unshielded conductor surface, showing a relationship to the electrical stress field. The void spaces were not, however, completely filled with this wax.

Cable Impregnated With Standard Compound

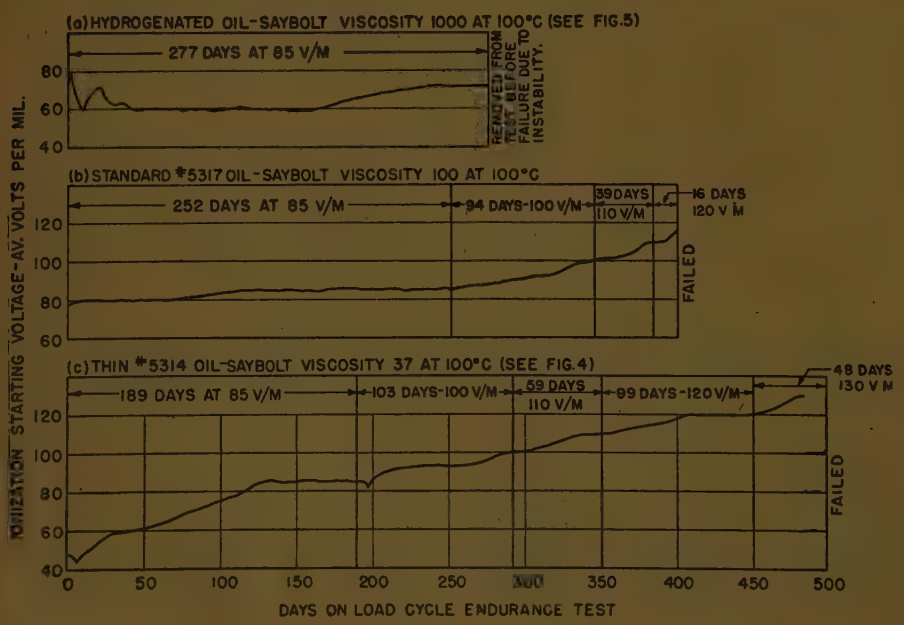
Typical load cycle endurance charts on gas-filled cable impregnated with standard number 5317 oil, having a Saybolt viscosity of 100 at 100 degrees centigrade were included in the two previous papers[1,2] and will not be repeated here. Initial ionization voltage of standard cable is given in Figure 3. On long-time load-cycle endurance, ionization has always been extinguished at each voltage step until the last, and final failure has occurred at an average of 120 volts per mil, ten pounds per square inch.

Dissection of the cable after failure has always shown wax or residue in the butt and strand spaces, with the paper tapes drier than initially, but not entirely so. The formation of wax has occurred just as frequently in the outer zone as in the inner. In this respect, there has not appeared to be any noticeable relationship with the electric-stress field.

Ionization Voltage on Endurance Test

At intervals of three or four days during the load-cycle endurance runs, just described, power-factor curves were measured over a sufficient voltage range to determine ionization starting voltage at that particular stage of the test. The results with the three different types of impregnating compound already described are charted in Figure 6. These three charts are of particular interest, since they give a direct measurement of critical void size at each stage of the load-cycle test from start to finish, and clearly bring out the automatic-grading and self-healing effects in mass-impregnated gas-filled cable as a function of compound viscosity. Charts *a* and *c* in Figure 6 correspond to Figures 4 and 5, relating, respectively, to load-cycle endurance runs with very thin and very heavy impregnating compounds. Chart *b* in Figure 6 corresponds to Figure 6 in the previous paper,[2] with standard number 5317 impregnating compound of intermediate viscosity.

Figure 6. Load-cycle endurance test on three-conductor cable at ten-pounds-per-square-inch nitrogen gas pressure, 350,000-circular-mil compact-sector conductors, no strand shielding, 0.200-inch insulation, showing ionization voltage increase due to automatic grading and self-healing action with impregnating compounds of three different viscosities



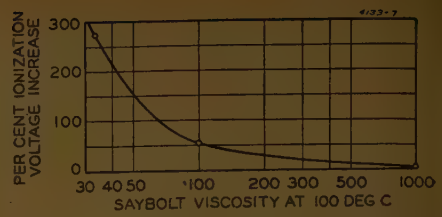


Figure 7. Showing relationship between viscosity of impregnating compound in gas-filled cable and automatic grading and self-healing action during load-cycle overvoltage tests as charted in Figure 6

Chart *a* shows that while very good initial impregnation was obtained with the high-viscosity hydrogenated oil, this cable was unstable from the start and had no self-healing properties at all, ionization starting voltage dropping below the initial value and never fully recovering. After 277 days at 85 volts per mil there was still a pronounced ionization discharge in this cable. It is possible that compounds of other types and of equally high viscosity would give better results, but it is our conclusion that impregnating compounds of extra-high viscosity have little if any self-healing properties in gas-filled cable and are undesirable.

Chart *b* in Figure 6 is typical of standard cable. The cable did not drain objectionably before leading and initial ionization voltage was normal. The cable shows stability from the start, with a steady self-healing action that extinguishes ionization at each voltage step until the last. In no case did ionization voltage drop below the initial value. On the last voltage step cumulative ionization finally predominated and failure occurred after 16 days at 120 volts per mil.

Chart *c* in Figure 6 indicates that while very thin oil imparts remarkable self-healing properties to gas-filled cable there is too much drainage of this oil from the insulation and initial ionization voltage is too low, being only 48 volts per mil at ten pounds per square inch, well below normal operating stress of 65 volts per mil. This caused instability at the start of the load-cycle endurance run, but automatic grading and self-healing action eventually overcame this and the cable settled down to a surprisingly long endurance run, ionization being extin-

guished at each voltage step until final failure at 130 volts per mil.

Figure 6 plainly shows a relationship between viscosity of impregnating compound and the ability of the cable to extinguish ionization discharge. This is made clearer in Figure 7, where the increase in final ionization voltage, at the end of the endurance run, over the initial value is given as a function of compound viscosity.

Summary of Conclusions

Further laboratory and field studies of gas-filled cable confirm that gas-pressure control gives very close control of performance characteristics.

Over the usual commercial range of conductor sizes and insulation-wall thicknesses, average stress rather than maximum stress determines performance of mass-impregnated gas-filled cable, and it is not necessary to specially strengthen the inner zone of insulation in the high-stress field near the conductor.

Strand shielding of mass-impregnated gas-filled cable has shown no marked benefit but since it is in no way objectionable it can be used when desired.

Extra thin paper tape in the high-stress field has shown no marked benefit in mass-impregnated gas-filled cable and its added cost and complications do not appear justified.

Extra-high-viscosity impregnating compound lacks self-healing properties and should not be used in gas-filled cable.

Thin impregnating oil has remarkable self-healing properties but drains too readily for safe use in gas-filled cable.

Optimum results are obtained with an intermediate impregnating compound of sufficiently high viscosity to be held in place by capillary attraction without draining objectionably but, at the same time, of sufficiently low viscosity to have mobility. Our experience has indicated that a Saybolt viscosity in the order of 100 at 100 degrees centigrade gives the best results.

References

1. Low-Gas-Pressure Cable, G. B. Shanklin, AIEE Transactions, volume 58, 1939, July section, pages 307–15.

2. Low-, Medium-, and High-Pressure Gas-Filled Cable, G. B. Shanklin. AIEE Transactions, 1942, October section, pages 719–26.

3. Ionization of Occluded Gases in High-Tension Insulation, G. B. Shanklin and J. J. Matson. AIEE Transactions, volume 38, 1919, pages 489–536.

4. The Electrical Strength of Nitrogen and Freon Under Pressure, H. H. Skilling and W. C. Brenner. AIEE Transactions, volume 61, 1942, April section, pages 191–5.

5. Characteristics of Oil-Filled Cable, G. B. Shanklin and F. H. Buller. AIEE Transactions, volume 50, 1931, pages 1411–20.

A New Frequency Relay for Power-System Applications

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Typical Applications

Loss of Tie-Line Overloading Plant Generation

THE interconnection of large power systems with industrial and defense plants having a comparatively small generating capacity is not unusual in these days. These plants require a considerable block of power for 24-hour full-capacity operation, and this power is available by means of the interconnection with the power system. More than one interconnection would assure higher continuity of service, but the general practice of locating defense plants in rural or suburban areas, remote from urban power-system networks, often results in one transmission-line interconnection operating in parallel with plant generators. Like all lines, the interconnection is subject occasionally to faults with resulting temporary loss of power for the plant. During these emergencies, the local generator or generators at the plant which normally contribute to the total load are suddenly called upon to carry all of the plant load. The local generators are not able to supply this load and consequently are heavily overloaded. As a result, the frequency decreases rapidly. However, complete shutdown of the plant can be avoided by dropping some of the load and retaining within the capacity of the local generators the load of the most essential machines and processes.

Thus, a reliable scheme is required to disconnect the load of the less essential feeders when the interconnection power is lost and the plant generators become overloaded. One scheme in general use is the application of an underfrequency relay in conjunction with reverse power relays. A typical system of this type is illustrated in Figure 1. When the power in the tie line decreases to a minimum, or reverses, and when the generator is overloaded as indicated by a drop in frequency, an auxiliary tripping relay is energized to disconnect the less essential feeders 1 through 4. Then the local generator can supply power to the more important feeders 5 and 6.

At the same time, the auxiliary relay also trips the tie breaker with the power system to prevent feedback of the local power to the power system. In the case of a fault on the interconnection line, the tie-breaker protective relays should complete the trip circuit ahead of the reverse-power and underfrequency relays. In this case, these latter relays operate as backup relays for the tie breaker. When power is available again via the interconnection, the two systems may be resynchronized, and the plant operator can restore the disconnected load in the proper operating sequence.

The measure of plant-system overload in this application is frequency change. The relay should respond to this change with a minimum variation in calibration because of voltage or temperature changes. The relay should be sensitive to frequency changes, operating fast for large changes in frequency and slower for small changes. Time delay is necessary to assure that the frequency relays

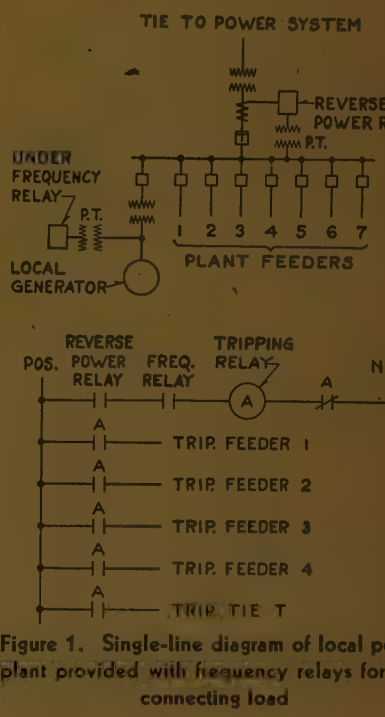


Figure 1. Single-line diagram of local power plant provided with frequency relays for disconnecting load

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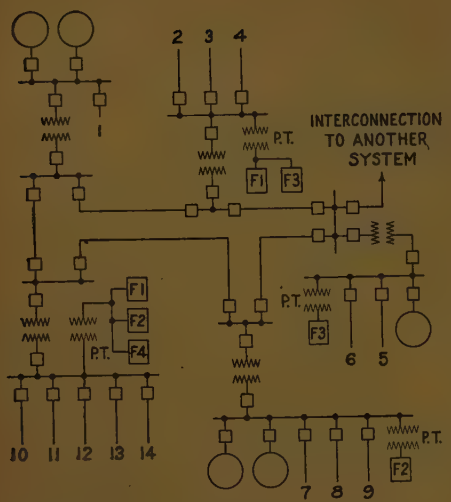


Figure 2. Single-line diagram of power system with frequency relays set for selectivity

Underfrequency Relays
(Normal Frequency—60 Cycles)

Symbol	Setting	Trip
F1	59.5 cycles	4 and 12
F2	59 cycles	9 and 14
F3	58 cycles	2 and 6
F4	57 cycles	10

operate only on sustained frequency changes, and not on changes resulting from faults which last only until the fault is cleared. For these reasons, an induction-type relay with inverse time characteristics is most suitable.

Loss of Generation on a Heavily Loaded System

Frequency relays can be used to disconnect load selectively on an overloaded system. A typical system consisting of several generating stations supplying local as well as remote loads is shown in Figure 2. Normally, this system operates at 60 cycles and near full capacity because of large increase in wartime loads. The loss of an important generating source or tie line or some other emergency system disturbance causes a sudden overload beyond the capacity of the remaining system, and the system frequency begins to drop. Underfrequency relays *F1*, set to operate at 59.5 cycles, disconnect feeders 4 and 12 at remote points on the system after a short time delay. The loss of this amount of load may be large enough to cause the frequency to return near 60 cycles. If not, and if the frequency continues to drop further, then frequency relays *F2*, set for 59 cycles, disconnect additional feeders 9 and 14 at other points on the system after more time delay. Again, the loss of this additional load may be enough to stabilize the frequency, but, if the frequency continues to drop, additional relays with lower-frequency settings disconnect more load. This process can be continued in a number of steps as dictated by the characteristics of the particular system and may be applied in the manner illustrated to drop varying amounts of load at a number of different stations.

Several relays may be set to operate at the same frequency value but to disconnect load at different time intervals. Thus, if the loss of the first block of load does not cause a definite rise in frequency, additional blocks of load would be dropped later, after suitable intervals of time. This has an advantage of only partially disconnecting the load assigned to be dropped at a particular frequency, if the frequency rises between the disconnecting intervals.

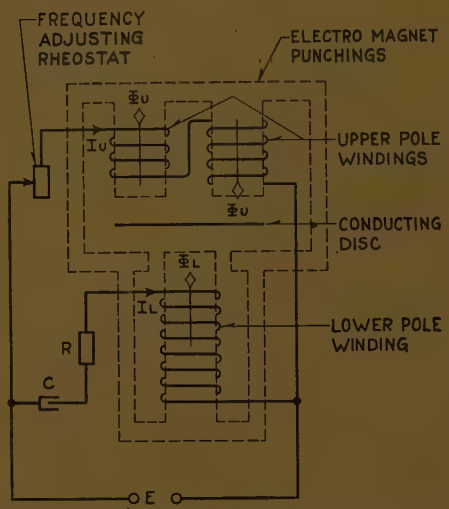


Figure 3. Schematic diagram of frequency relay

These schemes are not expected to replace dispatching or automatic load control, but provide a backup measure that can prove particularly valuable in holding important loads during an emergency. The scheme is applicable particularly to smaller systems such as are commonly found in rural areas, in large industrial plants, or a defense area whose overload capacity is limited, and may be exceeded by unpredicted increases in load.

The frequency relays in this scheme should be sensitive to changes in frequency, yet with a considerable range available for the selection of the operating frequency required in the various steps. In addition to the variable frequency setting, the relays should be capable of variable time settings over a wide range to permit selective timing of the various steps. As the relays may be located at different localities and operate under different conditions, it is desirable that the frequency relay have a minimum temperature and voltage error.

These applications of frequency relays discussed are typical and subject to variations and modifications required in each particular application. Other applications of frequency relays include overspeed or overfrequency protection of rotating equipment as well as underfrequency protection. The relays may not always be used only to trip circuit breakers, but also to give an alarm or warning of impending trouble.

Description of Relay

Relay Operating Principles

The preceding discussion has pointed out the operating requirements of typical systems using frequency relaying. These translate into several specific relay-performance requirements. The minimum trip-frequency variation of the relay should be small with respect to line-voltage change and with respect to ambient-temperature variation. The time and frequency setting of the relay should be easily adjustable over a wide range, and the time delay should be an inverse function of the frequency departure from normal system frequency. The relay described here satisfies these requirements and at the same time is quite simple in mechanical construction. Existing frequency relays use two electrical operating elements with opposing torques. The design described in this paper operates on an entirely different principle and utilizes only a single operating element.

In appearance, the relay is very similar to standard-type induction relays. It has the conventional rotating disk, time-lever scale, damping magnet, and a single electromagnet. Figure 3 is a circuit diagram of relay connections, and Figure 4 is a simplified vector diagram which illustrates the relay operation as either an under- or overfrequency device.

Essentially, the relay operation is comparable to that of a directional element. In Figure 4a, the upper-pole current, I_U, is shown lagging the impressed voltage, E. The lower-pole current, I_L, also lags the impressed voltage, but by an angle less than that of I_U. The lower-pole relay circuit, as shown in Figure 3, has inductance and capacitance in series, and, as the frequency increases, the lower-pole current becomes more lagging. Because inductive as well as capacitive reactance is present, the current in this circuit lags at a more rapid rate than the upper-pole current. At some frequency, the lower-pole current will lag sufficiently to be in phase with the upper-pole current. Under such

circumstances, the upper and lower fluxes of the relay are in phase, and there will be no torque on the induction disk. If the frequency now increases slightly, the lower-pole current and flux are rotated further in the clockwise direction, so that they lag the upper-pole quantities, and the induction disk will have a torque produced by the quadrature components of the upper- and lower-pole fluxes. The polarity of the overfrequency relay connections is so chosen that for ϕ_L leading ϕ_U, the relay has opening torque; then, when the frequency increases sufficiently for ϕ_L to lag ϕ_U, the torque of the relay reverses so that the contacts move in the closing direction. The larger the frequency increase beyond the zero-torque point, the greater the angular displacement between upper and lower fluxes, and the faster the relay operating time. Therefore, relay timing is inverse with respect to frequency changes.

The design is readily changed to an underfrequency circuit-closing relay by reversing the winding connection of either the upper or lower poles, which reverses the disk torques. To maintain open contact at normal frequency, the circuit constants are adjusted so that I_L lags I_U slightly, as shown by Figure 4b. As the frequency decreases, I_L advances, and, when it leads I_U, the contact closes.

The minimum trip frequency is determined by the point at which I_L and I_U are in phase; that is, zero torque. By changing the angle, with respect to the impressed voltage, at which this condition occurs, the frequency setting of the relay may be set as required. This angle is changed by adjusting the rheostat in the upper-pole circuit. Thus, in the case of the underfrequency relay, Figure 4b, as the resistance is increased, the upper-pole current lags less, so that a larger change in phase angle is required to swing I_L around to lead I_U. This corresponds to a lower minimum trip-frequency setting of the relay. A wide range of adjustment is easily realized by this means. Ten cycles is a typical frequency spread chosen.

Relay Operating Characteristics

The principles of relay operation already described permit the use of a single electromagnet and a mechanical construction which is easily adapted to the use of variable time-delay settings. These are obtained by the time-lever adjustments which permit variation of the moving-contact travel from a maximum of 180 degrees of rotation to a very small amount. Typical curves of relay operation for various lever and frequency settings are shown in Figure 5. These are taken for an instantaneous frequency drop. The wide range of settings available gives wide application limits.

The type of element described here inherently meets the requirement of minimum variation in tripping frequency with variation in impressed voltage. This may be seen if the relay is considered as a sensitive directional element operating near zero torque. At exactly zero torque, change in voltage will have no effect. The torque remains zero. Close to zero torque, the effect of voltage change on

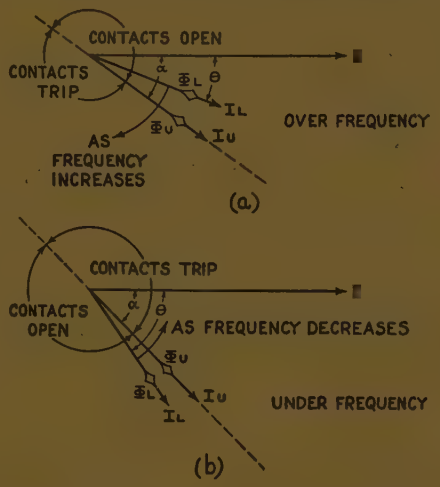


Figure 4. Vector diagrams showing principles of relay operation

the relay torque is still negligible, as shown in Appendix I.

The relay is extremely sensitive to frequency change. Consider the following equation for relay torque, and rate of change of torque

$$T = KI_L I_U \sin(\theta - \alpha) \quad (1)$$

$$\frac{dT}{d(\theta-\alpha)} = KI_L I_U \cos(\theta - \alpha) \quad (2)$$

where K is a factor of proportionality, and $\theta - \alpha$ is the phase angle between I_L and I_U. At zero torque, $\theta - \alpha = 0$. At this point the rate of change of relay torque with respect to phase angle is a maximum. Since $\theta - \alpha$ is a function of the frequency only, the relay will be at maximum sensitivity to frequency change.

The circuit elements of the relay are designed to make optimum use of the inherent sensitivity to frequency change and insensitivity to voltage change. The factors which determine the optimum design are the phase angle of the upper-pole circuit and the inductance and capacitance of the lower-pole circuit. The phase angle of the upper-pole circuit determines the angle at which zero torque occurs. The two extremes in design, occur for an angle of 0 degrees and an

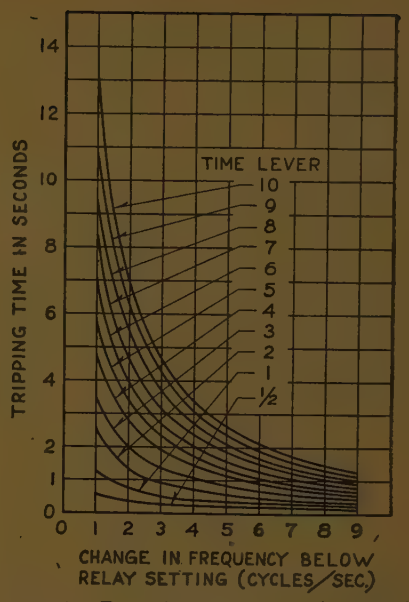


Figure 5. Typical time curves of 115-volt 50–60-cycle underfrequency relay

angle of 90 degrees. For the first case a large amount of external resistance is required to swamp out the inductive reactance of the relay coil, and hence only a small amount of operating ampere turns would be supplied the upper-pole windings. That is, the relay would not be a very sensitive directional element, so that this type of design is not satisfactory. The other extreme is for zero torque to occur at 90 degrees lag. Such a condition requires a purely reactive circuit, and the phase angle of such a circuit does not vary with frequency change. Thus, if any attempt were made to operate the relay with a zero-torque angle occurring near 90 degrees, the performance would be very unsatisfactory, since relay torque would respond in only small degree to change in frequency. It is apparent, therefore, that the phase angle of zero torque should be somewhere between these two extremes. An angle of 55 degrees is in fact the optimum one, and the relay is designed to operate near this value. This is more rigorously shown in Appendix II. With 55 degrees as the optimum zero-torque angle, the relative values of upper-pole circuit resistance and inductance are determined to be $R_U = 0.707\, X_U$.

The following considerations apply to the determination of the lower-pole-circuit constants. A circuit is required which changes vary rapidly in phase angle with respect to change in frequency. This is the case when a resistor is used in conjunction with reactive impedance whose magnitude varies very rapidly with frequency change. Such an impedance may be the series combination of inductance and capacitance. The re-

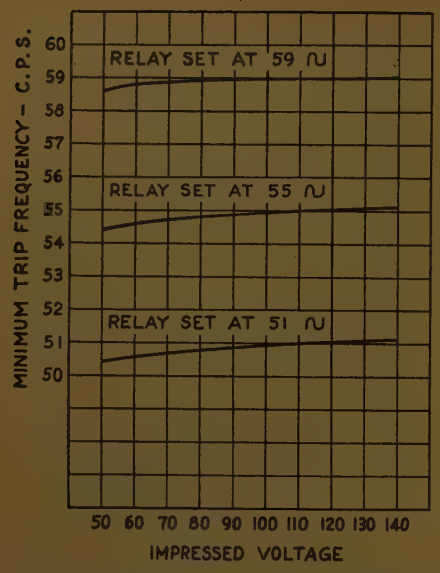


Figure 6. Typical voltage–frequency curves of 115-volt 50–60-cycle underfrequency relay

actance of such a combination is the difference $X_L - X_C$. Since X_L and X_C are inverse with respect to frequency variation, the difference will vary very rapidly if both X_L and X_C are very large and nearly equal. That is, the inductance of the lower-pole circuit should be as large as possible, and the capacitance as small as possible consistent with permissible burden and heating requirements. The resistance of this circuit is chosen approximately 0.707 times the net reactance so that the 55 degrees zero-torque-angle requirement is satisfied. Appendix II presents a more formal derivation of these requirements.

The variation of minimum trip frequency with respect to voltage change for a relay designed on the basis of the preceding criteria, is shown in Figure 6. For the complete range of settings, the frequency error is negligibly small for voltage variations from 50 to 140 volts. Note however that the errors are greatest

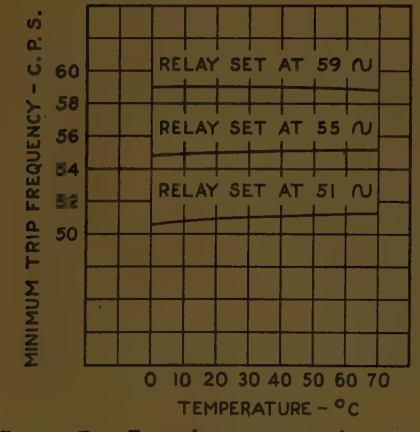


Figure 7. Typical temperature–frequency curves of 115-volt 50–60 cycle underfrequency relay

at the low-frequency settings. Here the largest value of upper-pole circuit resistance is required, and this represents the maximum departure from the optimum circuit requirements previously set forth.

The last consideration in determining the optimum design of the relay-circuit elements is the error due to ambient-temperature variation. This error can occur because of the variation of the inductive, capacitive, and resistive elements of the relay, with temperature. Since these variations cannot be eliminated entirely, the circuits must be designed so that the various changes tend to compensate each other. Variation in L of course is very small, and, since both upper- and lower-circuit inductance vary at the same time, any change in L merely will cause both current vectors I_L and I_U to rotate together. Thus the phase displacement between the two currents is not affected appreciably, and negligible errors result. This same principle may be applied to the resistance variation of the coil windings. However, with such an arrangement, there is no element in the upper coil circuit whose variation will compensate for the capacitor in the lower circuit. What is done therefore is to design the lower-pole winding with as small a coil resistance as possible relative to the upper-pole coil resistance. The physical construction of the electromagnet makes this relatively simple. The additional resistance placed in series in the lower-pole circuit to give optimum power factor produces a swamping action so that the over-all temperature variation of resistance in the lower-pole circuit is small. As the upper-pole resistance varies with temperature change (assuming negligible change in lower-pole circuit resistance) the vector I_U is swung around slightly. The direction is more lagging if the temperature decreases and less lagging if the temperature increases. C, in Figure 4, is a capacitor with a small negative temperature coefficient. That is, as the temperature decreases, C increases and X_C decreases. As the temperature increases, X_C increases. Thus I_L swings in the lagging direction as the temperature decreases and in the leading direction as the temperature increases. That is, the lower-pole current vector follows the upper-pole circuit vector, and hence the phase angle between the two changes but slightly with temperature variation, so that the frequency error due to ambient change is small. Typical temperature curves for a relay designed as described are shown in Figure 7. These indicate that the temperature error has been eliminated to a large extent.

Conclusions

This paper describes a new frequency relay with inverse time delay which trips at an assigned frequency with negligibly small voltage and temperature errors. The time delay of the relay, as well as the minimum trip frequency may be independently adjusted within a wide band. These design characteristics make the relay very suitable for power-system applications in which frequency is used as the discriminating quantity.

Appendix I

The following derivation indicates the order of magnitude of relay torque variation with respect to voltage near minimum trip frequency.

List of Symbols

E = voltage impressed on relay
R_L, L_L, C_L = resistance, inductance, capacitance of relay lower-pole circuit
R_U, L_U = resistance, inductance of relay upper-pole circuit
X_L = lower-pole circuit reactance $= \omega L_L - \frac{1}{\omega C_L}$
X_U = upper-pole circuit reactance $= \omega L_U$
$\dot{Z}_L$ = lower-pole circuit impedance $= R_L + j\left[\omega L_L - \frac{1}{\omega C_L}\right]$
$\dot{Z}_U$ = upper-pole circuit impedance $= R_U + j\omega L_U$
I_L = lower-pole current
I_U = upper-pole current
θ = phase angle by which I_L lags E
α = phase angle by which I_U lags E
$\omega = 2\pi F$
F = minimum trip frequency
T = relay torque
K = constant of proportionality

Assuming negligible shift between relay fluxes and respective currents, the equation of the induction disk torque is as follows:

$$T = KI_U I_L \sin(\theta - \alpha) \tag{3}$$

where $\theta - \alpha$ is the angle between I_U and I_L. Note that for $\theta > \alpha$, T is positive; $\theta < \alpha$, T reverses. However, only the absolute value of $\theta - \alpha$ is considered in this discussion, so that it is equally valid for under- or over-frequency relays.

$$\theta = \tan^{-1}\frac{X_L}{R_L} = \tan^{-1}\frac{\omega L_L - \frac{1}{\omega C_L}}{R_L} \tag{4}$$

$$\alpha = \tan^{-1}\frac{X_U}{R_U} = \tan^{-1}\frac{\omega L_U}{R_U} \tag{5}$$

$$I_L = \frac{E}{Z_L} = \frac{E}{\sqrt{R_L^2 + \left(\omega L_L - \frac{1}{\omega C_L}\right)^2}} \tag{6}$$

$$I_U = \frac{E}{Z_U} = \frac{E}{\sqrt{R_U^2 + \omega^2 L_U^2}} \tag{7}$$

The variation of torque with respect to oltage is given by

$$\frac{T}{E} = \frac{\partial}{\partial E}\left[\frac{KE^2}{Z_U Z_L}\sin(\theta-\alpha)\right] \tag{8}$$

$$\frac{T}{E} = \frac{2EK}{Z_U Z_L}\sin(\theta-\alpha)$$

$$\frac{T}{E} = \frac{2KI_U I_L \sin(\theta-\alpha)}{E} = \frac{2T}{E} \tag{9}$$

At minimum trip, the torque is only that mount required to overcome relay spring ension. Thus the numerator of equation 9 small and the denominator large, so that he variation of torque with respect to voltge is small.

Appendix II

The following derivations indicate optiium design requirements for the relay ciruit elements:

The relay sensitivity is measured by the agnitude of torque variation with respect o ω. This should be as large as possible.

If we refer to equation 3

$$\frac{T}{\omega} = \frac{\partial T}{\partial I_L}\cdot\frac{\partial I_L}{\partial \omega} + \frac{\partial T}{\partial I_U}\cdot\frac{\partial I_U}{\partial \omega} + \frac{\partial T}{\partial(\theta-\alpha)}\cdot\frac{\partial(\theta-\alpha)}{\partial \omega} \tag{10}$$

From equations 4, 5, 6, 7, $\partial(\theta-\alpha)/\partial\omega$, $\partial I_U/\partial\omega$ and $\partial I_L/\partial\omega$ are evaluated. From quation 3, $\partial T/\partial I_L$, $\partial T/\partial I_U$, and $\partial T/\partial(\theta-\alpha)$ re determined. ollecting terms

$$\frac{T}{\omega} = KI_L I_U \cos(\theta-\alpha)\times\left[\frac{R_L\left(L_L+\frac{1}{\omega^2 C_L}\right)}{Z_L^2} - \frac{R_U L_U}{Z_U^2}\right] - KI_L I_U \sin(\theta-\alpha)\times\left[\frac{X_L\left(L_L+\frac{1}{\omega^2 C_L}\right)}{Z_L^2} + \frac{X_U L_U}{Z_U^2}\right] \tag{11}$$

At minimum trip $\theta-\alpha$ is small, so that he second term of equation 11 may be eglected. Hence if we set $\cos(\theta-\alpha)=1.0$, nd note that at minimum trip

$$\cos\theta = \cos\alpha = \frac{R_U}{Z_U} = \frac{R_L}{Z_L} \tag{12}$$

we have

$$\frac{\partial T}{\partial \omega} = KI_L I_U \cos\theta\left[\frac{L_L+\frac{1}{\omega^2 C_L}}{Z_L} - \frac{L_U}{Z_U}\right] \tag{13}$$

$\partial T/\partial\omega$, and hence the frequency sensitivity can be made large by choosing L_L large, C_L small, and Z_L small. That is, the inductive reactance ωL_L, and the capacitive reactance $1/\omega C_L$, must both be large, but the resultant reactance $X_L=\omega L_L - 1/\omega C_L$ should be small. This will mean that

$$\frac{L_U}{Z_U} \ll \left[L_L+\frac{1}{\omega^2 C_L}\right]\frac{1}{Z_L} \tag{14}$$

The optimum value of R_L is determined by differentiating $\partial T/\partial\omega$, the measure of relay sensitivity, and setting the derivative equal to zero. This determines maximum sensitivity with respect to R_L.

If we substitute equations 6 and 12 in equation 13

$$\frac{\partial T}{\partial \omega} = \frac{KEI_U R_L}{Z_L^3}\left[L_L+\frac{1}{\omega^2 C_L}\right] - \frac{KEI_U}{Z_U}\frac{R_L L_U}{Z_L^2}$$

$$\frac{\partial^2 T}{\partial\omega\partial R_L} = KEI_U\left(L_L+\frac{1}{\omega^2 C_L}\right)\times\left[\frac{1}{(R_L^2+X_L^2)^{3/2}} - \frac{3R_L^2}{(R_L^2+X_L^2)^{5/2}}\right] - \frac{KEI_U L_U}{Z_U}\left[\frac{1}{R_L^2+X_L^2} - \frac{2R_L^2}{(R_L^2+X_L^2)^2}\right] = 0$$

$$\left[L_L+\frac{1}{\omega^2 C_L}\right]\left[\frac{Z_L^2-3R_L^2}{Z_L^3}\right] - \frac{L_U}{Z_U}\left[\frac{Z_L^2-2R_L^2}{Z_L^2}\right] = 0$$

$$R_L^2 = Z_L^2\left[\frac{\left(L_L+\frac{1}{\omega^2 C_L}\right) - L_U\frac{Z_L}{Z_U}}{3\left(L_L+\frac{1}{\omega^2 C_L}\right) - 2L_U\frac{Z_L}{Z_U}}\right]$$

$$\cos\theta = \cos\alpha = \frac{R_L}{Z_L} = \sqrt{\frac{\left(L_L+\frac{1}{\omega^2 C_L}\right) - L_U\frac{Z_L}{Z_U}}{3\left(L_L+\frac{1}{\omega^2 C_L}\right) - 2L_U\frac{Z_L}{Z_U}}}$$

$$\cos\theta = \cos\alpha = \sqrt{\frac{1}{3}}\sqrt{\frac{1-\frac{L_U}{Z_U}\cdot\frac{Z_L}{\left(L_L+\frac{1}{\omega^2 C_L}\right)}}{1-\frac{2}{3}\frac{L_U}{Z_U}\cdot\frac{Z_L}{\left(L_L+\frac{1}{\omega^2 C_L}\right)}}} \tag{15}$$

But from equation 14

$$\frac{L_U}{Z_U} \ll \frac{L_L+\frac{1}{\omega^2 C_L}}{Z_L}$$

or

$$\frac{L_U}{Z_U}\cdot\frac{Z_L}{L_L+\frac{1}{\omega^2 C_L}} \ll 1$$

Therefore, the second radical of equation 15 is approximately equal to 1, and we have

$$\cos\theta = \cos\alpha \approx \sqrt{\frac{1}{3}}$$

$$\theta = \alpha \approx 54.7^\circ \tag{16}$$

Therefore, $\tan\theta = 1.414$ or

$$\sqrt{2}R_L = X_L = \omega L_L - \frac{1}{\omega C_L} \tag{17}$$

To indicate the accuracy of this approximation, suppose

$$\frac{L_U}{Z_U}\cdot\frac{Z_L}{L_L+\frac{1}{\omega^2 C_L}} = 0.25$$

which means that

$$\left(L_L+\frac{1}{\omega^2 C_L}\right)\frac{1}{Z_L}$$

is only a nominal amount larger than L_U/Z_U. Even under these circumstances, the value of θ calculated from equation 15 is 56.7 degrees, which indicates that the approximated value of 54.7 degrees is very close for design purposes.

References

1. Inertia Relay "Anticipates" to Facilitate Reclosure, J. T. Logan, J. H. Miles. *Electrical World*, April 6, 1940, pages 69–72.

2. Factors Contributing to Improving Electric Service by Means of High-Speed Switching and Utilization of Stored Energy, J. T. Logan, J. H. Miles. AIEE Transactions, volume 60, 1941, December section, pages 1012–20.

3. Relay Protection of Tapped Transmission Lines, M. A. Bostwick, E. L. Harder. AIEE Transactions, volume 62, 1943, October section, pages 645–50.

Modern Motive Power for the Sorocabana Railway

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THE Sorocabana Railway in inaugurating electric traction on its extensive meter-gauge system has selected the 87-mile-long double-track section between São Paulo and Santo Antonio in the State of São Paulo, Brazil, for the initial electrification. Twenty locomotives are being furnished to handle passenger, freight, and mixed trains with scheduled speeds of 35 miles per hour for passenger, and 30 miles per hour for freight and mixed trains over a mountainous terrain involving maximum grades of two per cent, and prevailing minimum radius curves of approximately 800 feet.

These locomotives are the largest and most powerful 3,000-volt d-c meter-gauge units built to date and are among the outstanding examples of design and construction embodying all welded fabrication of mechanical parts and assemblies for main-line passenger and freight locomotives. Ten are being furnished by the General Electric Company and ten by the Westinghouse Electric and Manufacturing Company, with the mechanical portion of all 20 built by the General Electric Company. In addition to the mechanical portions which are identical, much of the equipment as furnished by the two companies is either identical or physically and electrically interchangeable. Pantographs, traction-motor blowers, watt-hour meters, and high-speed *JR* circuit breakers are identical with all pantographs manufactured by the Westinghouse Company and all breakers manufactured by the General Electric Company. Traction motors and motor generator blower sets are interchangeable. General characteristics of the locomotives are given in Table I. General dimensions are shown by Figure 1, and general appearance by Figure 2.

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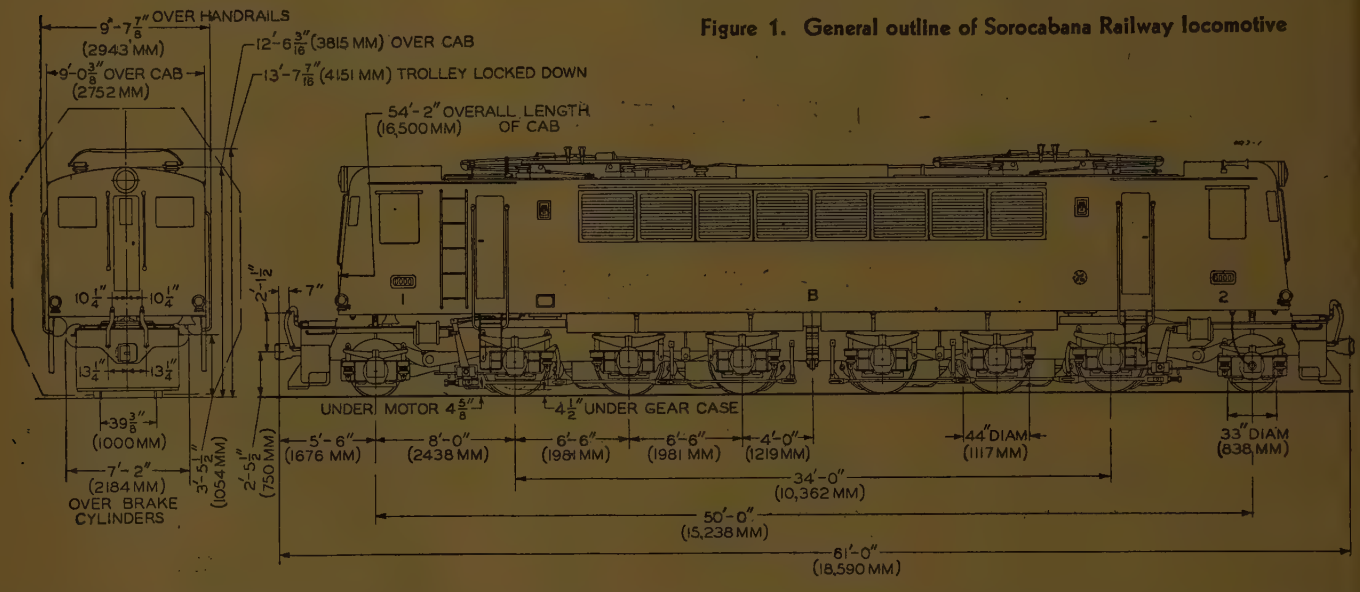


Figure 1. General outline of Sorocabana Railway locomotive

Running Gear

The running gear consists of two three-axle driving trucks (Figure 3) and two single-axle radius-bar guiding trucks (Figure 4). The driving-truck frames are articulated at their inner end by a ball joint arranged to permit angular movement in both vertical and horizontal planes. The outer ends extend over the guiding trucks and carry draft gear, coupler, and pilot. The frames are fabricated of steel plates and structural shapes throughout with all parts torch-cut to size requiring little or no machining (Figure 5). Side frames are universal mill-rolled open-hearth steel $3\frac{1}{2}$ inch thick, normalized, with an ultimate tensile strength of 55,000–65,000 pounds, maximum carbon content 0.25 per cent.

The guiding trucks are equipped with outside journals and rolled-steel wheels. Semielliptic and coil springs are arranged in series over the top of the journal boxes. A laterally movable bolster is provided on which a portion of the locomotive weight is carried through a system of equalizers extending from the springs over the outer driver boxes. The bolster is provided with a variable gravity-restraint device of relatively high initial restraint to steady the locomotive and minimize lateral oscillations when it is running on tangent track and to assist guiding of drivers negotiating track turnouts and curves (Figures 6 and 7). An extension of each guiding-truck inner frame serves as a radius bar connecting it to the main truck frame. These truck frames are also fabricated from torch-cut steel plates and structural shapes readily adapted to welding. The weight of the locomotive, with the exception of wheels, axles, journal boxes, and that part of the motors which is carried directly on the axles, is carried on a system of semi-elliptic and coil springs equalized to provide the desired loading on all wheels (Figure 8).

Cab Underframe

The cab underframe or platform is constructed of commercial steel shapes, bars, and plates. The structure is about 54 feet long by 9 feet wide. The center sills, which are the main load-carrying members of the frame, consist of two 18-inch



Figure 2. Three-quarter view of Sorocabana Railway locomotive

After the installation of apparatus and and wiring is complete, the compartment is lowered through the hatch opening in the cab and is welded securely to the underframe and roof structure.

Construction Details

The locomotive design incorporates throughout the use of fabricated sub-

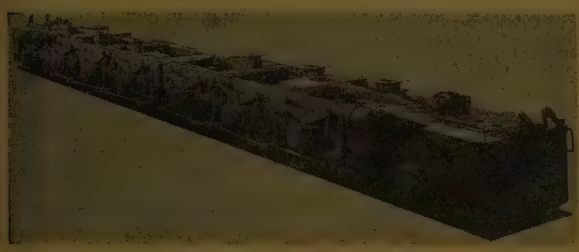
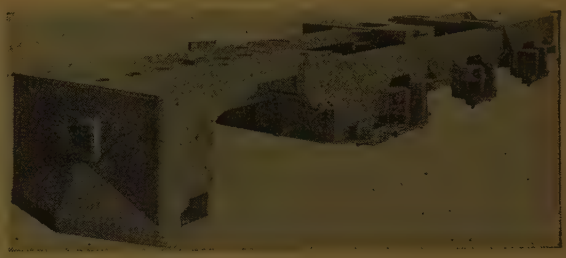


Figure 3 (left). Assembly of driving and guiding trucks complete

Figure 4 (below). Guiding truck

Figure 5. (right) Driving-truck frame

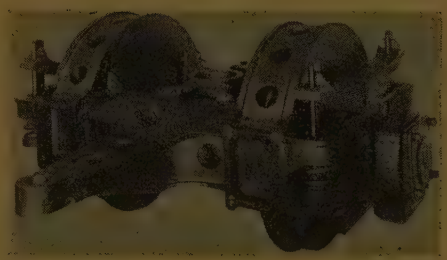


by 12-inch H beams reinforced with top and bottom plates of one-half inch thickness, forming a central duct serving as a pressure chamber into which air is discharged from two motor-operated blowers. From this chamber ventilating air is distributed to traction motors, accelerating resistors, and other electric apparatus. Side and end sills are made of eight-inch standard steel channels. Bolsters supporting center plates, side bearings, and lifting lugs are rigidly tied into center- and side-sill members. The 18-inch sill members extend well beyond the bolsters and are joined with eight-inch H beams (Figure 9) extending to ends of frame.

Cab

The cab is of semistreamlined design and is carried on two center plates located one on each of the two driving trucks. One of the center plates is arranged for sliding motion in the lower or truck socket to compensate for variations in truck center-plate distance when operating on curved track. Additional supports are provided through four spring-cushioned loading pads slideably supported, one on each side of each driving truck. The loading-pad springs are calibrated to give the desired load distribution on all driving and guiding wheels.

The cab consists mainly of exterior sheets 0.180-inch thick welded to a four-inch steel channel frame erected on the cab underframe. Louvres are provided in the side sheets of the central portion of the cab for ingress of air to the motor-blower sets. Large hatch openings are provided in the roof to facilitate installation and removal of apparatus and are



closed by means of bolted-down hatch covers.

Apparatus Compartment

The apparatus compartment is built as a separate unit to facilitate easy installation of control apparatus and wiring.

Table I. Characteristics of General Electric Locomotives for Sorocabana Railway

Classification 1−*C*+*C*−1−238/286−6 *GE* 734 *A*	3,000 volts
Length over-all, inside of coupler knuckles	61 feet, 0 inches
Width over-all	9 feet, 7 7/8 inches
Height over trolley locked down	13 feet, 7 7/16 inches
Track gauge	1 meter
Total wheel base	50 feet, 0 inches
Rigid wheel base	13 feet, 0 inches
Diameter driving wheels	44 inches
Diameter guiding wheels	33 inches
Total weight in running order	286,000 pounds
Weight on drivers	238,000 pounds
Weight per driving axle	39,666 pounds
Weight on guiding axles	48,000 pounds
Weight per guiding axle	24,000 pounds
Number of motors	6
Gear ratio	75/17
Continuous rating (horsepower)	1,910
One-hour rating (horsepower)	2,195
Continuous tractive effort (pounds)	24,475
One hour tractive effort (pounds)	29,500
Speed, continuous rating (miles per hour)	29.3
Speed, one-hour rating (miles per hour)	27.9
Maximum safe speed (miles per hour)	56



Figure 6. Guiding-truck bolster, center-plate extension, and parts of lateral-restraint device



Figure 7. Guiding-truck frame

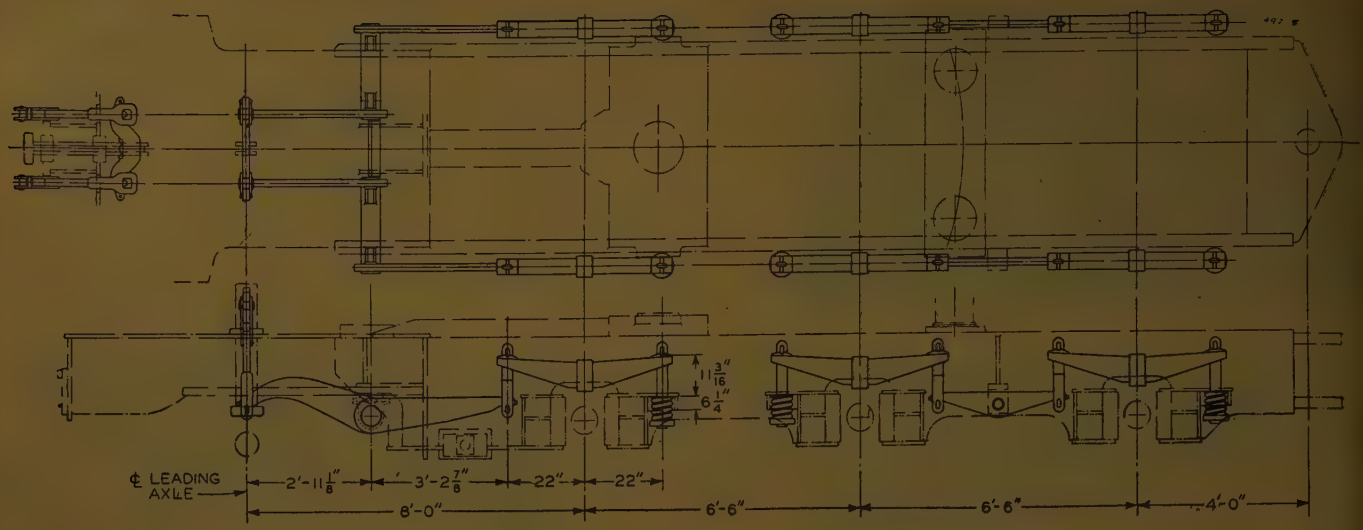


Figure 8. Spring and equalization system

assemblies which are easily handled for operations on the individual parts before being welded together for the complete assembly. All cross-tie plates fitting between side frames are machined to length with ends squared and are welded into subassemblies to facilitate the easy setting up and squaring of the main truck frames. Each side frame, the articulation or inner end frame, the main-frame extension and other similar groups of parts are likewise fabricated to form finished subassemblies before final assembly together. The boring of the recess for the cab center plate in the middle cross tie and also the boring of the inner end frames for the articulation are performed in the subassembly stage.

After the truck frame is otherwise assembled and welded, pedestal shoes, forming guides for journal boxes, are set to position in a special fixture. Shoes are accurately trammed to position for squareness and distance, laterally as well as longitudinally, to the frame center line extending through articulation, cab center plate, guiding-truck radius-bar fulcrum and center plate, draft gear and coupler. After accurate adjustments are accomplished the shoes are welded rigidly to the pedestal brackets. The cab sides, together with the cab frame, the hatches, cabinets, and compartments, likewise are first fabricated as subassemblies after which they are set up and assembled with the cab platform which is itself a complete subassembly.

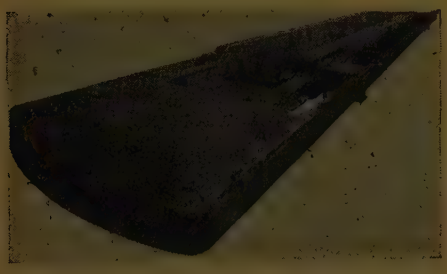


Figure 9. Cab underframe



Figure 11. Apparatus-compartment unit

Air-Brake Equipment

The air-brake equipment is a combination of vacuum and compressed-air systems with the locomotive brakes operated by compressed air and the train brakes operated by vacuum. The locomotive brakes may be operated independently of the train brakes, or in conjunction with them, in which latter case the train vacuum system acts as a pilot for the locomotive air-brake system. Two 12-inch-by-12-inch brake cylinders on each truck apply the brakes on that truck through a single brake shoe on each driving wheel. Interlocking is provided to prevent simultaneous operation of air brakes and regenerative braking to avoid

Figure 10. General arrangement of apparatus in cab

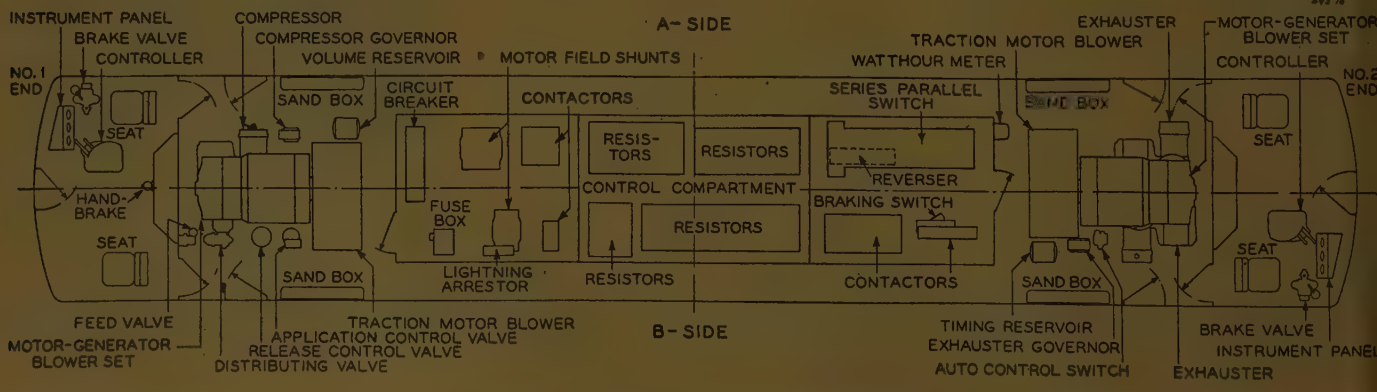




Figure 12. Traction-motor, wheel, and axle unit

the possibility of wheel sliding during regenerative braking.

Location of Apparatus

The space between the operator's cabs is divided into three sections. The apparatus compartment, completely enclosed but with access doors and with removable covers along the aisles on each side, is in the center, while auxiliary apparatus such as motor generator blower sets, compressors, exhausters, and air-brake control devices are located at each end. The operator's cabs at each end of the locomotive are separated from the apparatus compartment by bulkheads with a door opening onto an aisle on each side of the locomotive (Figure 10). All apparatus is arranged to provide maximum accessibility for inspection and maintenance. Figure 11 shows the apparatus compartment complete with all equipment being lowered into place in the main cab through the hatch opening in the roof.

Electric Apparatus

Two spring-raised air-lowered pantographs are provided, either of which has adequate capacity for collecting total locomotive current from the 3,000-volt overhead line. The pantographs are equipped with antifriction bearings, and each has two separate contact shoes, independently hinged, to provide greater flexibility in maintaining contact with the overhead contact wire. A small reservoir which can be charged from the control air system is provided for maintenance of air pressure for unlatching the pantograph when both pantographs are down and no other air pressure is available on the locomotive. A small hand-operated air pump is also provided for this purpose. The two pantographs are connected together through a cable mounted on insulators on the roof and to the main power circuits through the protective devices mounted in the apparatus compartment in the main cab.

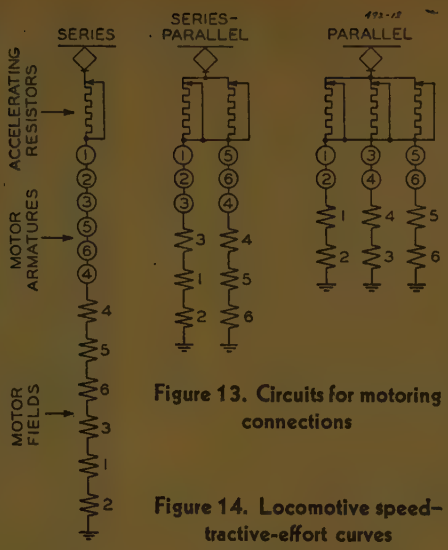
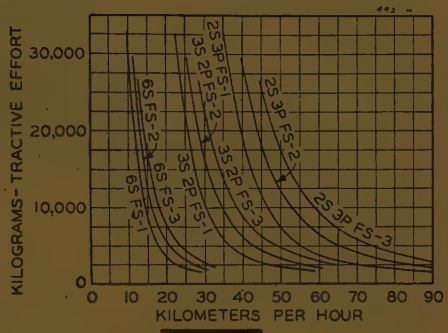


Figure 13. Circuits for motoring connections

Figure 14. Locomotive speed–tractive-effort curves



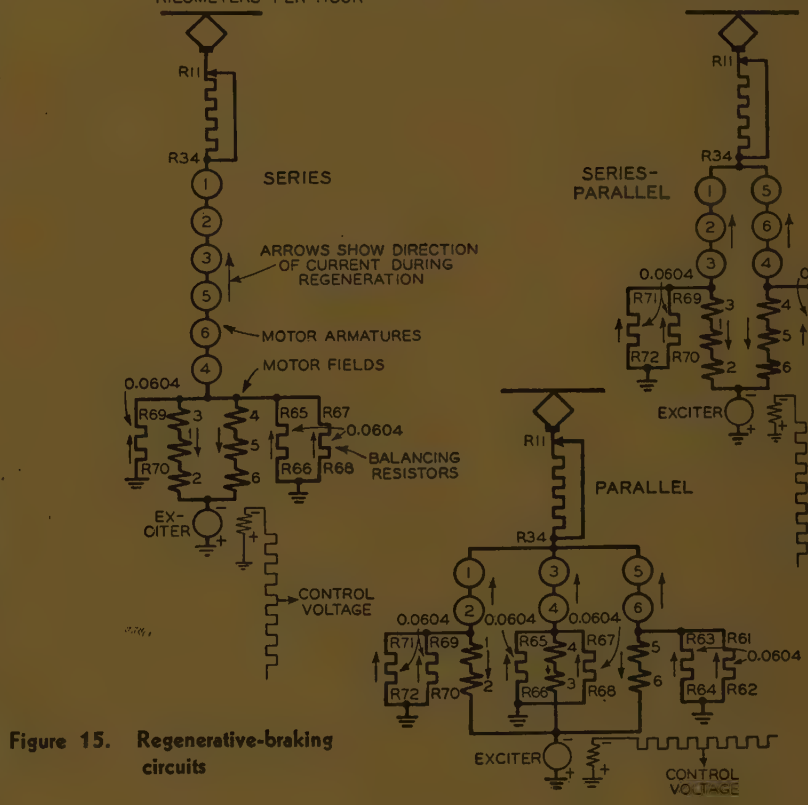


Figure 15. Regenerative-braking circuits

The traction motors are wound and insulated for operation two in series on 3,000 volts and are of the commutating-pole force-ventilated type with antifriction bearings. Figure 12 shows a traction motor in place and suspended on the axle with gear case in place. Axle-suspension bearings are waste-packed, oil-lubricated. The drive consists of single-reduction gearing with a pinion on the motor shaft engaging a solid gear on the axle, both being enclosed in a gear case which also serves as a reservoir for the gearing lubricant.

The motor circuits are arranged for three combinations of motors: six in series, three in series with two such groups in parallel, and two in series with three such groups in parallel. Accelerating steps and one full-field and two reduced-field running steps are provided for each motor combination, thus making a total of nine running steps in motoring (Figures 13 and 14). Regenerative braking is provided for each of the three combinations of motors (Figures 15 and 16).

The auxiliary equipment duplicates that previously used on locomotives for the Paulista Railway, and consists of two 3,000/65-volt motor generator blower sets, two 65-volt exhausters, and one 65-volt compressor.

Each motor generator blower set consists of a single-commutator series-wound commutating-pole 3,000-volt motor, direct-connected to a 65-volt generator with a blower rotor overhung on an extended shaft from the motor end of the set. The generators of the two sets furnish power for the operation of control, lights, compressor, and exhausters, with one of the sets serving as an exciter

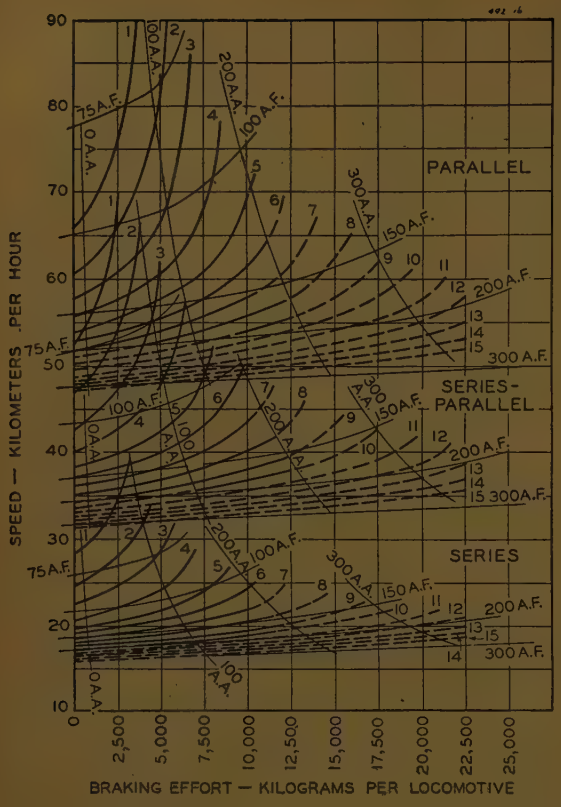


Figure 16 (left). Speed-braking-effort curves

Light lines marked *AA* and *AF* are constant armature- and field-ampere curves, respectively



Figure 17(right). *JR* circuit breaker

for traction-motor fields during regeneration. The blowers furnish ventilating air for all force-ventilated electric apparatus.

The exhauster control is so arranged that normally only one is in operation and at a reduced speed, while both operate intermittently at full speed to assist in quickly creating the necessary vacuum to effect the release of train brakes.

Control Equipment

The high-voltage control equipment is located in the apparatus compartment in the center of the locomotive and includes the accelerating resistors, braking resistors, main-circuit contactors, series-paralleling switch, reverser, *JR* circuit breaker, and other protective apparatus, switches, and fuses.

The low-voltage equipment is located in the two operating cabs and includes the master controllers, voltage regulator, pantograph magnet valves, control-circuit switches, and fuses and instruments.

Cast iron grids are used for the high-current sections of the accelerating resistors while those in the lower-current sections and in the braking circuits are formed of edge-wound strips of resistance alloy. All are insulated from their supporting frameworks which are mounted on porcelain insulators, thus providing double insulation to ground throughout.

The high-speed *JR* circuit breaker (Figure 17) protects the main circuits from short circuits and from overload through the medium of overload relays in the traction-motor circuits and also protects the equipment against overvoltage during regeneration through the medium of an overvoltage relay.

Manually operated starting switches are provided for operation of the two motor generator sets, and a selector switch is provided for alternative selection of the continuously operated exhauster. An automatic relay transfers the auxiliaries normally operating on the exciter motor generator set to the auxiliary motor generator set during regeneration.

The main circuit contactors, series-paralleling switch, reverser, and braking switch are electropneumatically operated by low-voltage control circuits energized in proper sequence from the master controller.

Each master controller is equipped with a main handle controlling accelerating and full-field running steps in motoring, a selector handle for selecting motor combinations for regenerative braking, a braking handle governing both regeneration and shunt-field steps in motoring, and a reverser handle for selecting direction of motion of the locomotive. All handles are interlocked mechanically to prevent improper operation and are conveniently located and in easy reach of the operator.

The instrument panel is mounted beneath the front window, directly in front of the engineer, and contains a line voltmeter, traction-motor field ammeter, traction-motor line ammeter, locomotive speed indicator, vacuum-brake gauge, and an air-brake gauge, illuminated indirectly from the rear of the panel.

The air- and vacuum-brake valves are on the right of the operating position, the master controller on the left. Bell-ringer and sander valves, control push buttons and valve for the pneuphonic horn are all mounted so as to require a minimum of movement on the part of the engineer. A record of power consumed during motoring and of power returned to the line when regenerating is obtained by means of a duplex watt-hour meter.

Voltage Transients in Arc-Furnace Circuits

AIEE SUBCOMMITTEE ON TRANSIENT VOLTAGES IN ARC-FURNACE CIRCUITS

THE subcommittee on transient voltages in arc-furnace circuits was established about four years ago to investigate the magnitude, cause, and control of transient voltages occurring at the primary and secondary terminals of arc-furnace transformers. The subcommittee has sponsored a number of field investigations during the past four years with the cooperation of steel companies, manufacturers of furnace equipment, and electric-equipment manufacturing companies. Only a part of the data obtained has been made generally available through publication (see a paper by Z. R. Whitehouse and C. C. Levy, "Surge-Voltage Tests on Electric Steel Arc Furnaces," presented at a joint meeting of AIEE and Association of Iron and Steel Engineers in Canton, May 1941, and also published references 1, 2, and 6). A data group of the subcommittee (L. W. Clark, chairman; S. B. Griscom, and E. R. Whitehead) was instructed to assemble all of the data available from the several field investigations, correlate the data as completely as possible to make them available for further study by the subcommittee, and prepare general conclusions regarding the magnitude of voltage transients found during the investigations. This paper summarizes the work of the data group and the preliminary findings of the subcommittee. It should be emphasized that this paper forms only a preliminary report of the work to date and is presented only so that the field data may be available for further study and analysis. The work of the subcommittee is continuing, not only in the collection and analysis of field data but also in experimental laboratory investigations and analytical analyses. It is expected that more definite recommendations regarding operating practices and possible protection schemes will be made after further study.

Paper 44-99, recommended by the AIEE committee on electrochemistry and electrometallurgy for presentation at the AIEE summer technical meeting St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 29, 1944; made available for printing May 8, 1944.

Personnel of subcommittee on transient voltages in arc-furnace circuits: J. E. Hobson, *chairman;* E. R. Whitehead, *secretary;* S. Arnold, E. W. Boehne, F. W. Brooke, L. W. Clark, L. E. Ferri. L. Fountain, C. M. Foust, S. B. Griscom, J. B. Hodtum, W. B. Kouwenhoven, C. C. Levy, W. E. Moore, J. O. Shimmin, N. R. Stansel, *past chairman;* F. J. Vogel, H. H. Wagner.

Installation Data

Table I lists the essential information concerning the 15 furnaces tested at 12 different locations, with references to the key-system diagram in Figure 1. The rated temperature rise as well as the kilovolt-ampere rating of the furnace transformer has been listed in order that the true thermal rating of the transformer be known. All of the installations except two were 55-degree-rise transformers. The two transformers rated at 40 degrees and 45 degrees rise, respectively, are thus proportionately larger than those with the same kilovolt-ampere rating at 55 degrees rise.

Transformers for the most generally used single-phase arc furnaces are in the 125-volt class, single-voltage operation, voltage range 90–125 volts.

Transformers for three-phase arc furnaces are in the 250-volt class, multiple-voltage operation. The maximum voltages are within the range 220 to 300 volts. Although the maximum voltages of large units are seldom below 250 volts, the voltages in the upper part of the range given are often used for small units. The selection of maximum voltage does not follow a size classification.

The grounding practice appears to be practically uniform. In all cases except one, the transformer tank and furnace shell are grounded and the secondary circuit is grounded through lamps. The data collected on the secondary-bus installations were incomplete, and only at 3 out of 12 installations was the actual spacing of the bus obtained.

Table II lists all essential data regarding the power supply to the furnace transformer. Sufficient information has been collected on the installations so that it should be possible to estimate the capacitance to ground of that part of the power-supply system directly affecting the furnace operation. Several of the tests indicate that the characteristics of the power-supply system may have a considerable effect on the magnitude and number of voltage transients experienced. It is desirable to have the power-supply data available for further study as to its possible effect upon the transients.

An indication is given as to whether or not there have been insulation failures on either the line or transformer side of the transformer breaker, or on the secondary bus. However, there is no definite assurance that voltage transients have caused each of these failures.

Test Data

Table III gives most of the test data collected at the different installations. The recording device in most cases was a Lichtenberg figure recorder which records both the magnitude and number of transients. In one of the tests a cathode-ray oscillograph was used, and in another test a vacuum-tube counter was used to record the number of transients that occurred above a certain magnitude.

Most of the tests were made with the recorder on the transformer side of the furnace breaker, but in a few cases the recorder was on the line side of the furnace breaker. Also, in most cases the recorder was connected line to ground, although

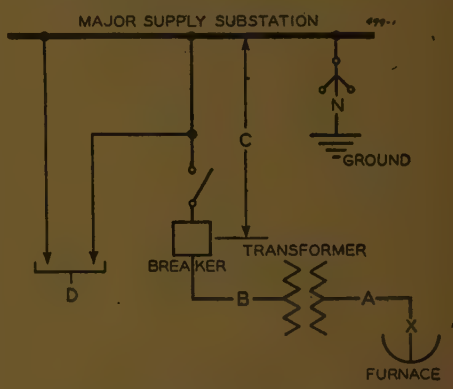


Figure 1. Schematic diagram of arc-furnace installation for reference—from Table II

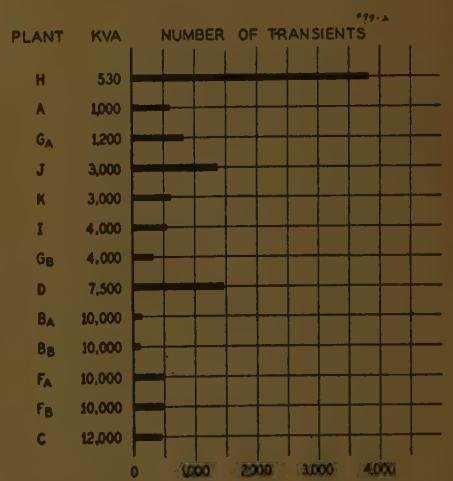


Figure 2. Total number of transient voltages per 1,000 hours of furnace operation plotted against furnace-transformer size

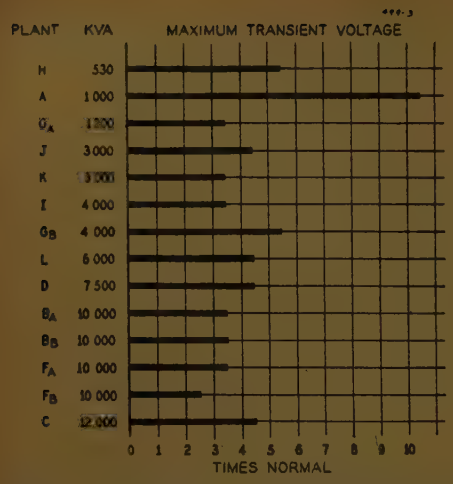


Figure 3. Maximum transient voltage (times normal voltage) plotted against furnace-transformer size

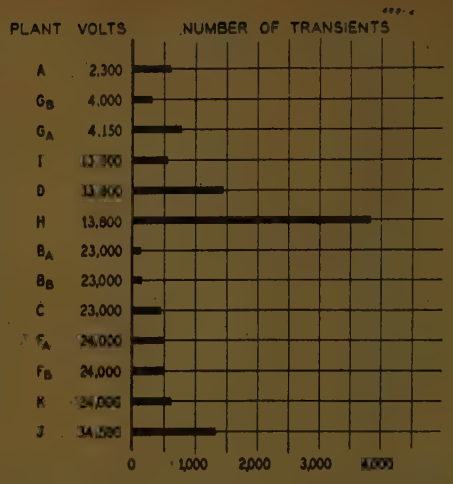


Figure 4. Total number of transients per 1,000 hours of furnace operation plotted against furnace-transformer primary voltage

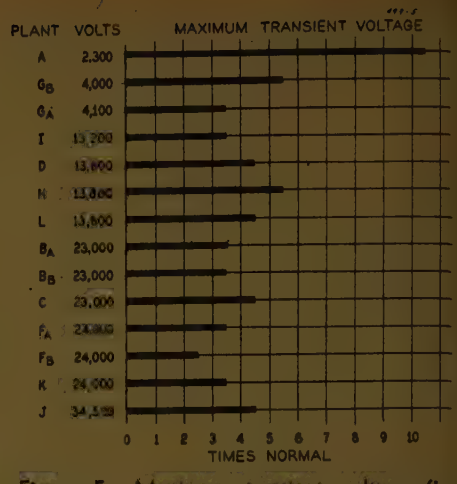


Figure 5. Maximum transient voltage (in times normal) plotted against furnace-transformer primary voltage

in a few cases it was connected line to line.

The transients recorded are segregated into groups according to magnitude. These groups are one to two times normal voltage, two to three times normal voltage, and so on, up to the highest group of 10 to 11 times normal. In all cases unless otherwise stated, this is crest voltage line-to-ground. The actual magnitude of normal crest voltage line-to-ground is given at each installation for convenient reference.

The transient-voltage distribution gives the number of transients per 1,000 hours operation in each class. For instance, plant *A*, test 1, had a total of 574 transients per 1,000 hours operation. The actual test covered a period of 768 hours. There were 30 transients between normal and twice normal voltage per 1,000 hours operation. In the top classification there were five transients per 1,000 hours operation in the 10 to 11 times normal-voltage class.

It was not possible to segregate the transient-voltage distribution for all tests; for instance, in test 2 at plant *A* it is known only that there were 77 transients between one and four times normal per 1,000 hours operation. Similarly, at installation *D* (tests 1, 2, and 3) it was not possible to give a complete breakdown. The tests at installation *E* were made with a vacuum-tube counter, so that the only information available is the number of transients in excess of 20 kv. For example, in test *E*—1, there were 33 transients per 1,000 hours of operation in excess of 20 kv, which is about twice normal voltage.

Test *E*—1 shows the relationship between number and magnitude of transients and breaker operations. The recording device was connected in such a way that it recorded transients only at the time the transformer breaker was closed or opened, and in this particular case 30 of 33 transients occurred at the time of closing the breaker, and only three occurred at the time of opening the breaker. There may have been other transients during normal operation of the furnace, but the equipment was not connected in such a way as to record them.

Test *E*—4 gives all of the transients during normal operation of the furnace, but an arcing ground developed in the cable feed to the transformer during the test, and it is believed that this caused the abnormal number of transients (1,660

Table I. Installation Data Regarding the Furnace Transformer, Circuit Breaker, Grounding, and Insulation of the Secondary Bus at Locations Where Tests Were Made

	Furnace Transformer					Voltage		Circuit Breaker				Grounding			Secondary Insulation		
Plant and Furnace Designation	Kilovolt-Amperes	Type	Rise (Degrees Centigrade)	Frequency (Cycles)	Phases	Primary Volts Delta	Secondary Volts Delta	Oil or Air	Amperes	Kilovolts	Interrupting Kilovolt-Amperes	Transformer Tank	Furnace Shell	Secondary Lamps	Spacing	Creepage	Material
A	1,000	OIWC*	55	60	3	2,300	230	Oil				Yes	Yes	Yes	Busses not interlaced		
Ba	10,000	OIWC	55	60	3	23,000	300	Oil	600	34.5	500,000	Yes	Yes	Yes	Busses interlaced		
Bb	10,000	OIWC	55	60	3	23,000	300	Oil	600	34.5	500,000	Yes	Yes	Yes	Busses interlaced		
C	12,000	OIWC	55	60	3	23,000	270	Oil	600	23		Yes	Yes	Yes	Busses interlaced		
D	7,500	OIWC	40	60	3	13,800	234	Air	600	15	250,000	Yes	Yes	Yes	Busses interlaced		
E	10,000	OIWC	55	60	3	11,000		Air	600	15	250,000	Yes	Yes	Yes	Busses interlaced		
Fa	10,000	OIWC	45	60	3	24,000	290	Oil	600	23	100,000 at 15 kv	Yes	Yes	Yes	½ inch	⅜ inch	Micarta
Fb	10,000	OIWC	55	60	3	24,000	240	Oil	600	23	250,000 at 15 kv	Yes	Yes	Yes	½ inch	⅜ inch	Micarta
Ga	1,200	OIWC	55	25	3	4,150	225	Oil				Yes		Yes			
Gb	4,000	OIWC	55	25	3	4,000	235	Oil				Yes		Yes			
H	530	OIWC	55	60	1	13,800	125	Oil		15		Yes	No	Yes			
I	4,000	OIWC	55	60	3	13,200	225	Air	600	15	250,000	Yes	Yes	Yes			
J	3,000	OIWC	55	60	3	34,500	220	Oil	600	46	500,000	Yes	Yes	Yes	¾ inch	¾ inch	Bakelite
K	3,000	OIWC	55	60	3	24,000	235	Oil	600	34.5	500,000	Yes	Yes	Yes	¾ inch	¾ inch	Bakelite
L	6,000	OIWC	55	60	3	13,800	265	Oil		34	500,000	Yes		Yes			

* Oil-immersed water-cooled.

Table II. Power-System Data

Refer to Figure 1 for Key Diagram

Plant and Furnace Designation	Neutral Grounding Method	Transmission Data B Overhead	Transmission Data B Underground	Transmission Data C Overhead	Transmission Data C Underground	Transmission Data D Overhead	Transmission Data D Underground	Insulation Failures A	Insulation Failures B	Insulation Failures C	Remarks
A		40 feet	No	4,400 feet	No	No	No	No	No	No	Single circuit
Ba								Yes	No	No	
Bb								Yes	No	No	
C		No	90 feet	840 feet	390 feet	6,200 feet	3,200 feet	Yes	Yes	No	Single circuit—arresters at both ends of *C* 500,000 kva short circuit at *B*
D	Ungrounded	40 feet	60 feet	1,000 feet	No	No	No	Yes	Yes	No	Single circuit—184,500-kva short circuit at *B* 3—15 kva, 460-volt capacitors at *A* 3—1/4 microfarads 13.8-kv capacitors and three type *S V* 13.8-kv arresters at *B*
E	Ungrounded delta	No	No	No	705 feet	No	2.76 miles	No	Yes	Yes	Three parallel circuits in *C* 12-kv Westinghouse distribution arresters at *C* 198,500-kva short circuit at *B*
Fa	Solid ground	80 feet	No	1,300 feet	1,300 feet	1,125 feet	1,150 feet	Yes	No	No	Single circuit 850,000-kva short circuit at *B* Westinghouse type *L V* 500-volt Arresters at *A* General Electric 24-kv—oxide-film arresters at *C*
Fb											
Ga	Solid ground	50 feet	No	No	1,500 feet	No	3,000 feet				Single circuit 100,000-kva short circuit at *B*
Gb											No arresters
H	8 ohms resistance	No	400 feet	2.4 miles	No	12.4 miles	2.9 miles	Yes	No	No	Single circuit 15-kv line-type arresters at *C* 120,000-kva short circuit at *B*
I	2.5 ohms resistance	No	100 feet	No	1,700 feet	No	25				Single circuit—no arresters 131,500-kva short circuit at *B*
J	Petersen-coil tests 1, 2, 4; solid ground test 3			4 miles	650 feet	350 feet	No	No	No	Yes	Oxide film arrester at *C* Underground cable at *C* bypassed by overhead for test 4
K	Solid ground	No	No	4 miles	No	No	No	No	No	No	25-kv "porous block" autovalve arrester at *C*
L	2 ohms resistance	No	0.25 mile	3 miles	16 miles	11 miles	165 feet	No	No	Yes	*B*-2 circuits, 2 cables each *C*-4 circuits, 2 cables each 26-kv deion tubes on one phase at *C* 550,000-kva short circuit at *B*

transients per 1,000 hours). There is a somewhat similar comparison between tests *E*—2 and *E*—3, except that it should be noted that the recording device on these tests was on the line side of the breaker. During test *E*—2, there were 11 transients (per 1,000 hours of operation) above twice normal during normal operation; when the recorder (test *E*—3) was connected to record transients only at the time of closing or opening the breaker, no transients were recorded.

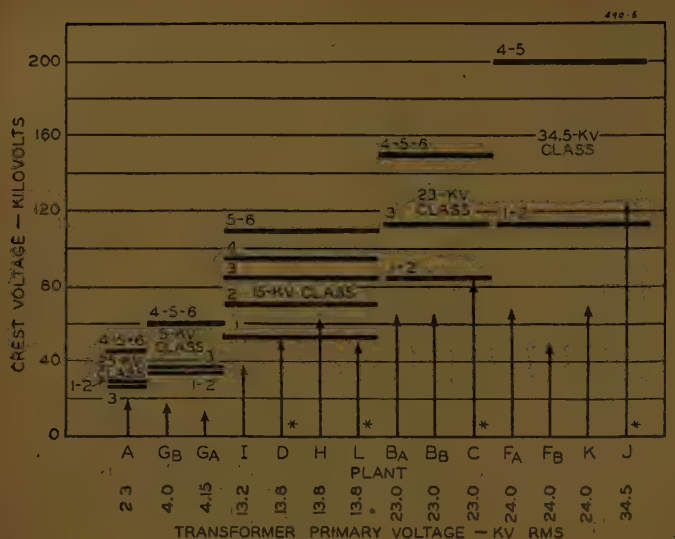
Tests at the *J* and *K* installations also give some information as to the effect of switch openings and closings on the transients. For instance, test *J*—1 shows 1,316 transients per 1,000 hours of operation, and during the test of 250 hours there were 700 switch operations. This indicates a total of 2,800 switch operations for the 1,000-hour period, and 47 per cent of the switch operations could have caused transients. However, test *J*—2 recorded transients only at the time of staged switching operations; and for 672 staged operations there were 315 transients which is also 47 per cent of the operations. This is at least an indication that most of the transients on the transformer side of the primary breaker at this installation occurred at the time of a switch operation.

A similar comparison for tests 1 and 2 at location *K* shows that under normal operating conditions there were transients for 30 per cent of the switch operations and that for the staged switching operations there were transients recorded on 34 per cent of the switching operations. These tests seem very definitely to link the number of primary transients recorded to the number of switch operations.

Under column headed "Test Conditions," an indication is given as to whether the test was conducted under normal operating conditions or under some special condition with perhaps some remedial measures having been taken or some form of corrective equipment added. Tests 1 to 8 at location *D* show the effect of various combinations of arresters, capacitors, and reactors. For instance, with no remedial measures there were 1,460 transients per 1,000 hours operation, and with various combinations of corrective measures both the number of transients and their magnitude were reduced in varying proportions, depending upon the measures used. Similarly, at two tests, *J*—2 and *J*—3, Petersen-coil grounding was replaced with solid grounding, showing both a slight reduction in the number of transients and a slight reduction in the magnitude of the transients. However, the greatest reduction occurred when a 650-foot length of cable in the primary feed to the installation was replaced with overhead, as shown in test *J*—4. This indicates that the electrical characteristics of the circuit have a definite effect on the magnitude and number of transients.

There are only two installations at which transient measurements were made on the secondary circuit of the furnace transformer. These data are summarized in Table V. At location *F* measurements were made on two 10,000-kva furnaces and at location *H* on a single-phase 530-kva furnace. The number of hours operation for the test at location *H* is unknown, so the number of transients could not be reduced to a 1,000-hour operation basis.

The secondary transients are divided into classifications reading directly in kilovolts rather than times normal. It is



Maximum transient voltage crest of 60-cycle one-minute test:
1—Apparatus bushings, small
2—Apparatus bushings, large
3—Air switches and bus supports

Impulse 1.5x40 full wave
4—Apparatus bushings, small
5—Apparatus bushings, large
6—Air switches and bus supports
* Insulation failures occurred

Figure 6. Maximum transient voltage appearing on primary winding of the furnace transformer compared with withstand test voltages of insulation for indoor equipment

thought that there would be little significance in classifying them on a times-normal basis where the actual secondary voltages vary only between 125 and 300 volts. The first column, for instance, in the transient-voltage distribution are those transients between 1 and 2 kv, 2 and 3 kv, and so forth, on up to the final column for the class 12 to 14 kv. Again the number of transients is given per 1,000 hours of operation.

Test *Fa*—3 covers 250 hours operation with the recorder located line to ground and with secondary arresters connected to the bus. There were no transients recorded during this period. Similarly, test *Fa*—6 gives a 720-hour run on the same furnace one year later with secondary arresters, and again no transients were recorded. However, test *Fb*—4 gives the number of transients recorded on the secondary winding of another transformer of practically duplicate rating and fed over practically a duplicate circuit. The only difference is that there were no secondary arresters in the circuit. This test showed 86 transients per 1,000 hours operation with one transient as high as eight or ten-kilovolt crest voltage line-to-ground. Test *Fb*—5 is another test run on the same furnace under similar conditions but with secondary arresters added. This shows 72 transients, but the magnitude has been reduced, and the great majority of transients are in the one- to two-kilovolt class, none higher than the three- to four-kilovolt class, indicating considerable improvement due to the arresters.

About one year later a similar test run was made on the same furnace with the transient recorders connected line to line instead of line to ground, and a vacuum-tube recording device was added to indicate arrester operation. Magnetic links were placed in the arrester circuits to give a measure of the transient current passed by the arresters. The test was conducted for 720 hours, and no transients were re-

Table III. Summary of Test Data on Primary-Voltage Transients

Plant and Furnace Designation and Test Number	Recording Device 1, 2, or 3*	Recorder Location (Line-to-Ground)	Test Operating (Hours)	Furnace Size (Kilovolt-Amperes)	Normal Line Voltage (Rms)	Normal Line-to-Ground Crest Voltage	Total Transients Per 1,000 Hours	Transient-Voltage Distribution—Number of Transients Per 1,000 Hours Operation—Times Normal Line-to-Ground Crest Voltage: 1-2	2-3	3-4	4-5	5-6	6-7	7-8	8-9	9-10	10-11	
A—1	1	*B*	768	1,000	2,300	1,870	574	30	305	126	48	30	21	5	3		1	5
A—2	1	*B*	456	1,000	2,300	1,870	77	←	77	→								
Ba—1	1	*B*	1,000	10,000	23,000	18,700	81	7	64	10								
Bb—2	1	*B*	1,000	10,000	23,000	18,700	78	15	62	1								
C	1	*B*	840	12,000	23,000	18,700	420	118	161	36	4							
D—1	1	*B*	150	7,500	13,800	11,200	1,460	←	1,453	→	0	7						
D—2	1	*B*	48	7,500	13,800	11,200	417	←	417	→								
D—3	1	*B*	912	7,500	13,800	11,200	1,170	←	1,169	→	1							
D—4	1	*B*	3,960	7,500	13,800	11,200	540	475	56	9								
D—5	1	*B*	418	7,500	13,800	11,200	42	37	5									
D—6	1	*B*	1,056	7,500	13,800	11,200	125	117	8									
D—7	1	*B*	1,416	7,500	13,800	11,200	208	136	68	4								
D—8	1	*B*	696	7,500	13,800	11,200	292	236	56									
E—1	3	*B*	336	10,000	11,000	8,950	33	←			33	(30 out of 33 were on closing breaker)						
E—2	3	*C*	551	10,000	11,000	8,950	11	←			11							
E—3	3	*C*	336	10,000	11,000	8,950	0											
E—4	3	*B*	240	10,000	11,000	8,950	1,660	←			1,660							
E—5	3	*C*		10,000	11,000	8,950		←			1	(Staged test—automatic trip in single-phase short circuit)						
Fa—1	1	*B*	2,711	10,000	24,000	19,500	500	475	24	1								
Fb—2	1	*B*	713	10,000	24,000	19,500	478	465	13									
Ga—1	1	*B***	84	1,200	4,150	5,850**	107	72	35									
Ga—2	1	*B*	84	1,200	4,150	3,380	798	179	584	35								
Gb—4	1	*B***	168	4,000	4,000	5,640**	209	143	48	18								
Gb—5	1	*B*	250	4,000	4,000	3,250	292	108	124	44	12	4						
H—1	1	*B*	44	530	13,800	11,200	3,810	1,630	1,520	523	114	23						
H—2	1	*B***	44	530	13,800	19,500**	2,412	1,230	955	227								
I—1	1	*B*	72	4,000	13,200	10,800	528	472	55	1								
J—1	1	*B*	250	3,000	34,500	28,000	1,326	784	280	196	56							
J—2	1	*B*		3,000	34,500	28,000		188	67	47	13***							
J—3	1	*B*		3,000	34,500	28,000		60	23	6***								
J—4	1	*B*		3,000	34,500	28,000		25	8***									
K—1	1 and 2	*B*	150	3,000	24,000	19,500	600	480	100	20								
K—2	1 and 2	*B*		3,000	24,000	19,500		168	70	7***								
L—1	2	*B*		6,000	3,800	11,200		15	3	3	1***							

* 1. Lichtenberg figure. 2. Cathode-ray oscillograph. 3. Vacuum-tube counter. ** Line to line.
*** Actual number of transients—not per 1,000 hours of operation.

corded. As far as could be determined, there was no change in furnace operation from the test of a year previous when a large number of transients was experienced; the only known difference was that the recorders were connected line to line instead of line to ground.

There is nothing brought out by the secondary tests on these installations to indicate that there is any connection between secondary transients and primary-breaker operations. This installation had four cases of secondary-bus flashovers, all of which occurred during the normal heat run of the furnace at a time when the primary breaker was closed and the furnace was in normal operation, indicating that some of the transients at least are not associated with breaker operation.

Figures 2 through 5 are presented to give a visual indication of the possible existence of any uniform pattern or relationship among furnace size, primary voltage, and the number and magnitude of transients. In all cases the data used are those of tests made when no remedial measures were taken and no corrective equipment added. These charts show no trends nor any functional relationship existing between the previously listed variables. This tends to strengthen the previously expressed thought that electric-system characteristics are a predominant factor in the number and magnitude of transients.

Figure 6 shows the magnitude, in kilovolts, of the maximum transient measured for each test and its relation to the withstand test voltage of insulation for indoor equipment for each voltage class. The AIEE Standards for apparatus bushings of both large and small apparatus, and for air switches and bus supports specify certain withstand test voltages which are a measure of the basic insulation level for each voltage class of insulation. There are some inconsistencies in the values specified in the Standards for the different types of apparatus, but, in general, an insulation level can be indicated whose lower bound is the crest of the 60-cycle one-minute withstand test voltage, and whose upper bound is the impulse withstand test voltage. It is believed that the actual insulation strength against furnace transients of this type, although certainly variable, falls somewhere between these limits.

At the installations where insulation failures were recorded (plants *D*, *L*, *C*, and *J*), the maximum transients all approach or pierce the lower bound of the insulation level shown on the chart. In this connection it should be pointed out that complete information is lacking regarding the actual insulation levels existing at each test installation, and that, if materially lower levels are encountered, infrequent failures might be expected. Insulation levels recommended for general use in power installations also appear to be satisfactory for most furnace installations. For some devices there are "high" and "low" levels within the nominal voltage rating, and the "high" level should be selected (particularly when the operating voltage approaches the "voltage class"), or it may be desirable to use apparatus of a higher actual insulation level or to apply suitable protection equipment.

Table IV. Summary of Test Conditions

Plant and Furnace Designation and Test Number	Test Conditions
A—1	No remedial measures
A—2	High-voltage capacitor at *B*
Ba—1	No remedial measures
Bb—2	No remedial measures
C	No remedial measures
D—1	No remedial measures, no capacitors, no arresters
D—2	High-voltage arresters, no capacitors, 5.9 per cent reactor
D—3	High-voltage arresters, low-voltage capacitors, no reactor
D—4	High-voltage arresters, low-voltage capacitors, 5.9 per cent reactor
D—5	High-voltage arresters, high-voltage capacitors, 5.9 per cent reactor
D—6	High-voltage arresters, high-voltage and low-voltage capacitors, 5.9 per cent reactors
D—7	High-voltage arresters, high-voltage and low-voltage capacitors, no reactor
D—8	High-voltage arresters, high-voltage capacitors, no reactor
E—1	Transients in excess of 20 kv recorded; transients only at time of opening or closing breaker
E—2	Transients in excess of 20 kv recorded; normal operation all transients
E—3	Transients in excess of 20 kv recorded; transients only at time of closing or opening breaker
E—4	Transients in excess of 20 kv recorded; transients evidently caused by arcing ground in cable at *C*
E—5	Transients in excess of 20 kv recorded
Fa—1	Normal conditions
Fb—2	Normal conditions
Ga—1	Normal conditions
Ga—2	Normal conditions
Gb—4	Normal conditions
Gb—5	Normal conditions
H—1	Normal conditions
H—2	Normal conditions
I—1	Normal conditions
J—1	Normal conditions (700 switch operations); Petersen-coil grounding
J—2	672 staged switching operations; Petersen-coil grounding
J—3	206 staged switching operations; solid grounding
J—4	328 staged switching operations; Petersen-coil grounding; 650 feet of cable replaced with overhead
K—1	Normal conditions (300 switch operations)
K—2	700 staged switching operations
L—1	31 staged switching operations, opening magnetizing current

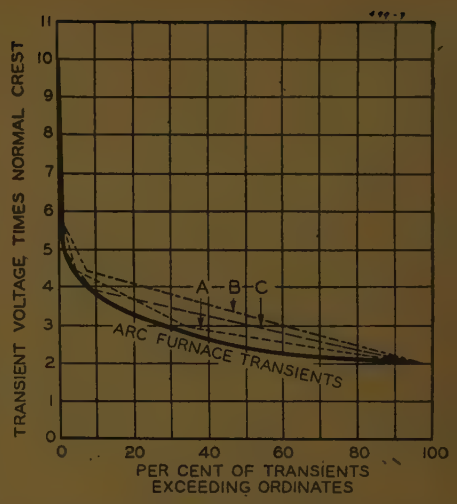


Figure 7. The distribution and magnitude of arc-furnace primary-voltage transients compared with switching and fault transient voltages

A and *C*—Switching transients
B—Transients from faults
From reference 4

Table V. Summary of Transient Voltages Recorded on Secondary Circuits

Plant and Furnace Designation and Test Number	Recording Device 1, 2, or 3*	Recorder Location Line-to-Ground	Test Operating (Hours)	Total Transients Per 1,000 Hours	Transient-Voltage Distribution—Number of Transients Per 1,000 Hours Operation—Crest Voltage in Kilovolts										Test Conditions
					1–2	2–3	3–4	4–5	5–6	6–7	7–8	8–10	10–12	12–14	
Fa—3	1	*A*	250	0											With secondary arresters
Fb—4	1	*A*	1,028	86	40	26	13	4	0	1	1		1		With secondary arresters
Fb—5	1	*A*	530	72	64	2	6								With secondary arresters
Fa—6	1	*A***	720	0											With secondary arresters
Fb—7	1	*A***	720	0											With secondary arresters
H—3	1	*A*			1	2	2	4	5	6	10***	10	2	1	Normal operation

* 1. Lichtenberg figure. 2. Cathode-ray oscillograph. 3. Vacuum-tube counter. ** Line to line.

*** Actual number of transients—not per 1,000 hours of operation.

Figure 7 shows the magnitude of arc-furnace primary transients as recorded on the various tests and compares them with switching transients and transients which may be caused by system faults. The dotted curves *A*, *B*, and *C* are the switching transients and transients from faults taken from the AIEE paper,[5] "Power-System Transients Caused by Switching and Faults" (see also Lewis and Foust[6]). This curve indicates that, as far as the primary transients recorded on these tests are concerned, they are of about the same order as, and at least not greater in magnitude than switching transients. There was only one test that showed transients in excess of five to six times normal. Of all the transients recorded above twice normal, 92 per cent were less than four times normal voltage. This curve sheet does not bring out the known fact, however, that the frequency of occurrence of furnace transients can be many thousands of times greater than that of ordinary power-system transients.

Summary

1. Voltage transients encountered in primary circuits of furnace transformers have characteristics similar to those of ordinary power-system voltage transients, except that of frequency of occurrence. Primary-circuit transient overvoltages in arc-furnace installations occur during the melting-down operation and at the time of opening or closing the furnace breaker. The highest magnitudes appear to be associated with breaker opening operations. Accordingly, there are more transients in furnace transformer circuits over a given period of time, because furnace breakers operate 40 to 50 times per day compared to a few times a year for the general use of circuit breakers.

2. Insulation levels recommended for general use in power installations also appear to be satisfactory for most furnace installations. For some devices, however, there are wide variations of the insulation level for the same voltage rating, and in those cases where the operating voltage approaches the "voltage class," it may be desirable to use apparatus of a higher actual insulation level as compared to the rating or to apply suitable protective equipment such as capacitors and lightning arresters.

3. There does not appear to be any functional relationship between either furnace size or primary voltage and the number and magnitude of transients on a times normal basis.

4. The magnitude of secondary transients suggests that they originate from current discontinuities in the secondary circuit.

5. This report does not cover internal transformer voltages.

6. Although this report is primarily a factual presentation of test data on voltage transients, some members of the committee thought it desirable to stress the use and adequate maintenance of a mechanically rugged circuit breaker especially selected for arc-furnace service.

Recommendations for Future Analysis and Study

The magnitude of furnace transients, both primary and secondary, appears to be very closely associated with the characteristics of the electric system, and it may prove worth while to investigate this relationship by future calculation and theoretical analysis. It is believed that sufficient power-system data have been tabulated along with the test data to permit such a study by an analysis group.

Wherever the point has been checked, the addition of high-voltage capacitors has been found to reduce both the number and magnitude of the transients recorded, and this fact may support the view that system characteristics are an important factor.

References

1. Suppression of Surges on Arc Furnaces, W. R. Stewart, S. B. Griscom, J. E. Hobson. *Westinghouse Engineer*, May 1941, page 11.

2. An Investigation of Overvoltages Associated With Arc-Furnace Operation, L. V. Black, E. W. Boehne. *Iron and Steel Engineer*, September 1941.

3. Klydonograph Surge Investigations, J. H. Cox, P. H. McAuley, L. G. Huggins. AIEE Transactions, volume 46, 1927, pages 315–29.

4. Power-System Transients Caused by Switching and Faults, R. D. Evans, A. C. Monteith, R. L. Witzke. AIEE Transactions, volume 58, 1939, August section, pages 386–94.

5. Lightning Investigations on Transmission Lines—II, W. W. Lewis, C. M. Foust. AIEE Transactions, volume 50, 1931, pages 1139–46.

6. A Study of Voltage Transients in Arc-Furnace Circuits, J. B. Hodtum, J. B. Rice. AIEE Transactions, volume 62, 1943, August section, pages 556–62.

7. The Geometry of Arc Interruption—II, E. W. Boehne. AIEE Transactions, volume 63, 1944, pages 375–87.

A New Carrier Relaying System

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It has long been realized that the surest and most effective way to obtain simultaneous breaker operations at both ends of a transmission line, regardless of the location of a fault within the line section, is by use of some form of pilot channel relaying. Two forms of this type of protection have been developed and used very successfully for a number of years. One system is the familiar distance-type carrier relay system, and the other is the single-element differential pilot-wire system.

This paper describes a new one-to-three-cycle carrier relaying system which combines the simplicity of pilot-wire protection with the versatility of power-line carrier.

A comparison of the characteristics of the distance-type carrier and pilot-wire relaying systems gives the background and reveals the impelling motives for the development of this new system. The distance type of relaying requires a source of current and potential to determine the location of a fault. Backup protection is provided, and auxiliary elements are required to prevent tripping on an out-of-step condition. Consequently, the contact and circuit complication has been increased over that required for a basic carrier relaying system.

In the single-element pilot-wire system, which has been in use for a number of years, certain problems were solved which resulted in definite steps toward simplicity. The chief progress toward simplicity in this scheme was effected by the use of relays operating on symmetrical components of current. This obviated the necessity of using separate relays in each phase and reduced the relay requirements to one per terminal to handle all phase and ground faults. This same symmetrical-component device also reduced the number of pilot wires to two.

No potential source is required for this pilot-wire system, since the positive- and zero-sequence components of the fault current at each end of the protected lines are compared.[1] System out-of-step conditions do not tend to trip the relays and, therefore, do not require auxiliary elements to prevent tripping. The resistance of the pilot wire is one limiting condi-

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tion, however, for this relay. Furthermore, in some cases induction in the pilot wire and differences of ground potential cause undesirable effects.[5]

It is evident from the foregoing that a marked simplification in a carrier relaying system is possible by using the elements of the pilot-wire relay and substituting a power-line carrier channel for the pilot wire. A carrier relaying system of such a nature has been developed, and extensive tests on an artificial transmission line in the laboratory and on an actual line in the field have proved it to be dependable in operation.

Principle of Operation

Basically the relays determine whether a fault is inside a protected line section or external to it by comparing, over the carrier channel, the relative directions of the currents at the ends of the line section. This comparison takes place in the grid circuit of a vacuum tube connected as shown in Figure 1. The tube is normally biased to cutoff; that is, the bias voltage E_c is sufficient to block the flow of plate current when no other voltage is present in the grid circuit. Carrier is transmitted from both ends of the line on alternate half-cycles of the power frequency. A voltage proportional to the received carrier is introduced into the grid circuit of the vacuum tube in series with a single-phase voltage derived from the local line currents. The polarities of these two voltages are such that for an internal fault current flows through the vacuum-tube plate circuit on alternate half-cycles of the power frequency. A voltage is thus induced into the secondary winding of transformer *TR* which is sufficient to operate the relay element *R* and close the trip circuit. For an external fault, the phase of one of the voltages in the grid circuit reverses, and the flow of plate current is continuously blocked. Therefore, no voltage is induced in the transformer secondary winding, and the relay does not operate. This vacuum tube is appropriately called a "relay tube" because of its function, and it will be referred to as such hereinafter.

Complete System

Figure 2 shows a complete basic diagram of one terminal of the relaying system. The three-phase line currents energize the positive-plus-zero-sequence filter. The single-phase output of the sequence filter is connected to a fault detector and to the grids of two thyratrons *V*1 and *V*2. These thyratrons and the associated re-

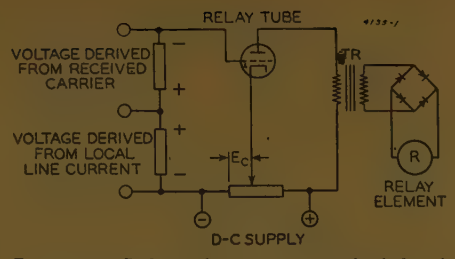
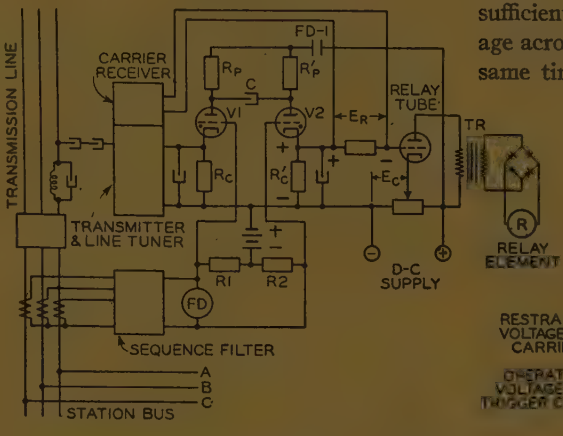


Figure 1. Relay tube circuit in which local and distant currents are compared

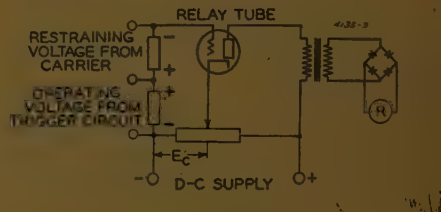


sistors and capacitors comprise a trigger circuit which is operated by the filter output voltage. The fault detector is used to obtain an intermittent carrier scheme, so that the carrier channel is available for any auxiliary function such as communication or telemetering except during the time of a fault.[4] One contact on the fault detector (*FD*-1) starts carrier through the thyratron trigger circuit, and another contact (not shown) is used to interrupt any auxiliary function.

Upon the occurrence of a fault, the contact of the fault detector closes and applies positive direct voltage to the plate circuits of *V*1 and *V*2. When the grid of *V*1 becomes positive, as a result of the filter output voltage, it fires and its plate or anode current very rapidly rises to a steady-state value as determined by the supply voltage and the plate and cathode resistors (R_p and R_c) of the tube. On the next half-cycle, the grid of *V*2 becomes positive, and thyratron *V*2 fires. When this occurs, the charge on the capacitor *C*, connected between the plates of *V*1 and *V*2, makes the plate potential of *V*1 momentarily negative which extinguishes *V*1. Thus the wave form of plate current of either thyratron will be square half-cycles of fixed magnitude for any value or wave form of grid voltage, providing, of course, sufficient grid voltage is present to fire the thyratrons *V*1 and *V*2. The voltage drop across the cathode resistor R_c is used as the plate-supply voltage for the oscillator tube in the carrier transmitter. Applying this voltage to the plate of the oscillator causes the transmission of carrier on alternate half-cycles of the power frequency.

The voltage drop across the other cathode resistor R_c' of thyratron *V*2 is connected in the grid circuit of the relay tube in series with the d-c output of the carrier receiver (E_R) as shown. When *V*2 fires, the voltage drop across R_c' makes the grid of the relay tube positive. The d-c output of the carrier receiver is connected with the opposite polarity and is sufficient to cancel the effect of the voltage across R_c' if carrier is received at the same time that *V*2 is conducting. The

Figure 2 (left). Complete basic diagram of simplified carrier relaying system



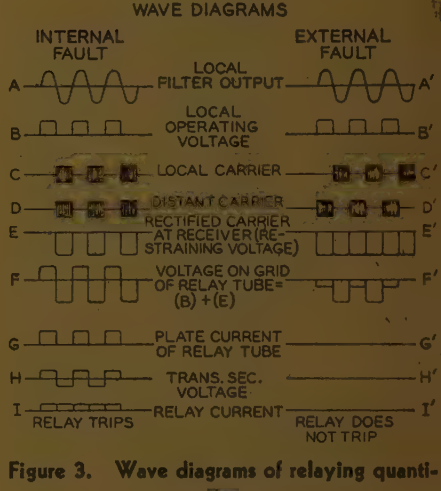


Figure 3. Wave diagrams of relaying quantities

remainder of the relay-tube circuit is the same as shown in Figure 1. The carrier transmitter and receiver are conventional carrier relaying units whose construction and principle of operation are well known.

Response to Faults

Figure 3 shows in detail how the circuits just described determine whether an internal or external fault exists. The relay-tube circuit is repeated at the top of Figure 3 for convenience. The "operating voltage from trigger circuit" is the voltage drop across R_c' of Figure 2. It is called operating voltage, as it makes the grid of the relay tube positive, causing the flow of relay-tube plate current. The

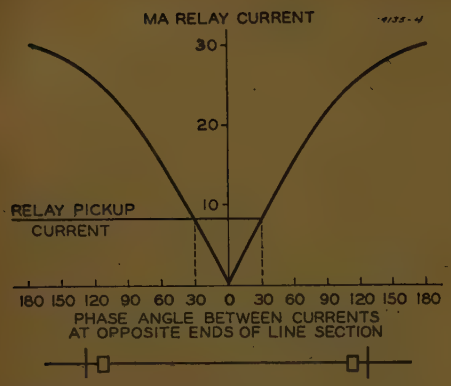


Figure 4. Typical phase-angle–relay-current curve of system

"restraining voltage from carrier" is the d-c output of the carrier receiver (E_R) and is so named as it opposes the operating voltage and prevents the flow of plate current in the relay tube by applying negative potential to its grid.

To see how the quantities shown in Figure 3 are utilized, consider first an internal fault. The polarities of the sequence-filter output voltages at the two ends of the line are such that, when the fault detectors operate, carrier is transmitted from both line terminals on the same half-cycles (Figure 3, *C* and *D*). The received and rectified carrier restraining voltage (Figure 3, *E*) is applied to the grid of the relay tube in series with the operating voltage (Figure 3, *B*). The net voltage on the grid of the relay tube is the sum of these two voltages, and has the wave form indicated at *F* on Figure 3. The reference axis at *F* is the voltage E_c required to cut off the plate current of the relay tube. Consequently, when the net grid voltage is above this axis, plate current flows. For an internal fault, there is no rectified carrier voltage during the half-cycles that the grid of the relay tube is made positive by the operating voltage. As a result, plate current will flow in the relay tube, as shown at *G* in Figure 3. The half-cycle plate-current pulses flow through the primary winding of the transformer *TR*. The secondary voltage (Figure 3, *H*) is rectified to provide a steady relay operating force, and the direct voltage so obtained causes current to flow to operate the relay *R* (Figure 3, *I*).

During an external fault, when the fault detectors pick up, carrier is transmitted from the two ends of the line on alternate half-cycles, as shown at *C'* and *D'* in Figure 3. This change occurs because the current at one end of the line has reversed with respect to the current at the opposite end. If the current entering the line is in phase with the current leaving it, the rectified carrier at each receiver will provide a substantially continuous restraining voltage (Figure 3, *E'*). This voltage in series with the operating voltage (3, *B'*) gives a resultant value which is always more negative than the cutoff bias of the relay tube (Figure 3, *F'*). As a result, no plate current flows (Figure 3, *G'*), and the relay does not operate.

The phase position of the line currents theoretically may fall anywhere between the inphase position and the 180-degree out-of-phase position. Figure 4 shows the characteristics of the relay over this entire range. As the phase position changes from in phase (external fault) to out of phase (internal fault), the blocks of the distant carrier change in phase position with respect to the blocks of local carrier, the two extremes being shown in Figure 3, *C* and *D*, and Figure 3, *C'* and *D'*. Between these two extremes the plate current of the relay tube will be blocked over more and more of the cycle, and the rms value of the a-c component of the plate current will decrease. This will result in a decrease of the current in relay *R* as shown on the typical phase-angle relay-current relationship Figure 4. By adjusting the sensitivity, the relay *R* can be set to operate when the angle between the currents at the two ends of the line becomes any desired value, such as 30 degrees, as shown.

The preceding explanation is based on single-frequency operation of the carrier equipment. That is, the transmitters and receivers at both ends of the line are tuned to the same carrier frequency. It should be noted, however, from a comparison of Figure 3, *B* and *C*, that carrier is transmitted locally only on the half-cycles when there is no operating voltage present. Consequently, it is never necessary to receive the local carrier signal, since the relay-tube plate current always will be zero during these half-cycles. This means that two-frequency operation of the carrier transmitters and receivers is possible without complication, if it is desired to use some auxiliary function which requires a two-frequency channel. In two-frequency operation, the transmitter at one end of the line and the receiver at the opposite end are tuned to one carrier frequency, and the other transmitter and receiver are tuned to a different frequency. This permits simultaneous transmission and reception in both directions as would be needed for telemetering in one direction and load control in the opposite direction. When two-frequency operation is used, the line trap at a given station is tuned to the frequency transmitted from that station.



Figure 5. Type-*HKB* relay used in simplified carrier relaying system described in paper

Special Considerations

Although the settings of the fault detectors at both line terminals usually will be the same, there is the possibility of a distant external fault picking up the fault detector at only one end of the line. As blocking depends on receiving a signal from the opposite end of the line section, carrier must be started at both line terminals before operating voltage is applied to the relay tube at either end.

To do this, the thyratron which starts carrier is set more sensitively than the one which provides operating voltage to the relay tube. The carrier-controlling thyratron also is more sensitive than the fault detector and therefore always will fire whenever the fault detector picks up, but the thyratron which provides operating voltage is set less sensitively and therefore will not fire until the fault current reaches a value approximately 50 per cent above the fault-detector setting. Thus, if the fault current is just above the pickup value of the fault detector, it will pick up, and thyratron *V*1 (Figure 2) will fire and cause the transmission of a continuous carrier signal. The two thyratrons will not trigger back and forth until the fault current is high enough to fire *V*2. Then the system will operate in its normal way. The firing point of *V*2 need be enough above that of *V*1 to insure that the fault detectors at both ends of the line pick up and start carrier before operating voltage is applied to the grid of either relay tube. The difference in

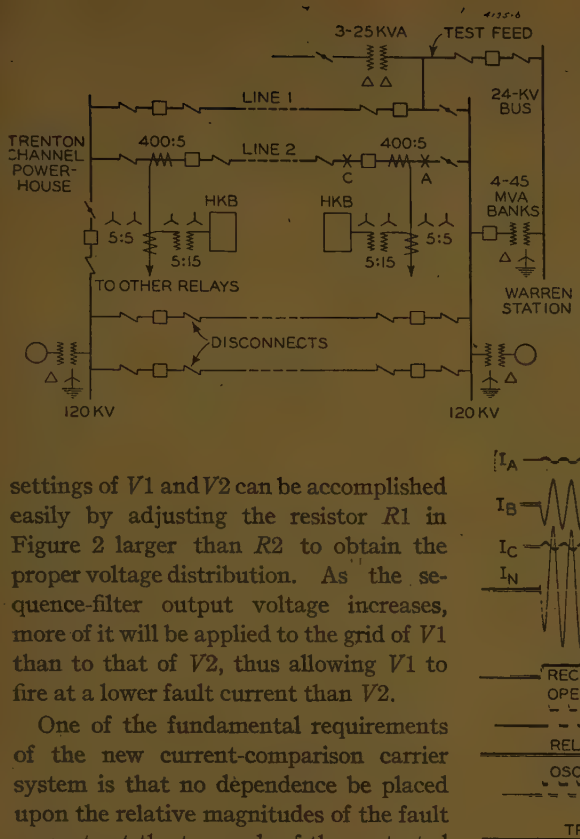


Figure 6 (left). Transmission-system conditions for staged tests

Figure 7 (right). Oscillogram of operation during internal fault

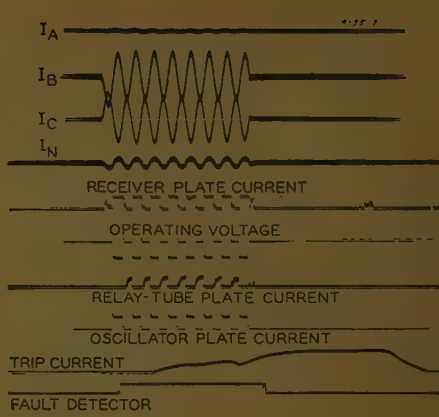
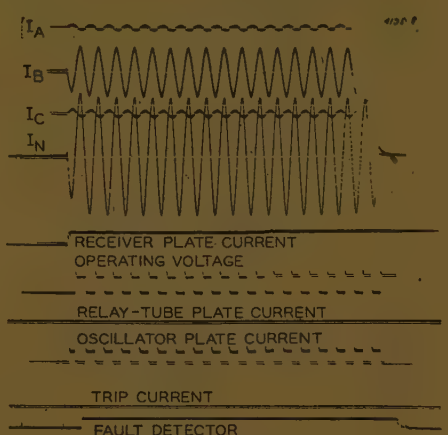


Figure 8 (below). Oscillogram of operation during external fault



settings of $V1$ and $V2$ can be accomplished easily by adjusting the resistor $R1$ in Figure 2 larger than $R2$ to obtain the proper voltage distribution. As the sequence-filter output voltage increases, more of it will be applied to the grid of $V1$ than to that of $V2$, thus allowing $V1$ to fire at a lower fault current than $V2$.

One of the fundamental requirements of the new current-comparison carrier system is that no dependence be placed upon the relative magnitudes of the fault currents at the two ends of the protected line section. In this respect the system operates somewhat differently from the pilot-wire relay in that it is not a ratio-differential scheme but operates only on the relative phase position of the currents at the two ends. The reason why magnitude comparison cannot be used is that to do so would entail the reception of a carrier signal through an internal fault. Also, the attenuation of a power line to carrier frequencies changes with weather and system switching conditions, resulting in the reception of a signal which varies in strength over a wide range. While some signal, no doubt, could be received through an internal fault, especially if high power were used, it seems fairly obvious that no dependence could be placed on the reception of a signal of the proper magnitude. In the present system, therefore, the circuits which control tripping and blocking are saturated at as low a current value as possible, and only the variation in phase position is utilized to discriminate between internal and external faults. Also, it is only necessary to receive a signal from the far end of a line section when blocking is required, that is, during a fault external to the protected section.

The carrier receiver saturates at a small value of received signal and provides sufficient restraining voltage to give a good margin of safety over the operating voltage over a very wide range of carrier attenuation. This is done to insure dependable blocking for external faults. It should be noted in Figure 3, C, D, and E, that the restraining voltage due to the reception of both the local and distant signals is no greater than for the reception of the distant signal alone (Figure 3, C', D', and E'). This is due to the saturation of the carrier receiver. As a result of saturating all the quantities involved, a single curve (Figure 4) suffices to show the relay-system characteristic over a wide range of fault currents.

On lines with a source of power at only one end, this system will block for an external fault beyond the line terminal having no source of power. However, for an internal fault, only the end having a source of power will trip. This operation is the same as for a standard distance-type carrier relaying system.

The application of this relaying system to three-terminal lines has been considered, and there appears to be no fundamental reason why it will not operate satisfactorily.[6] However, to date no actual tests on a three-terminal line have been made.

There is a theoretical condition where the use of the positive-plus-zero sequence filter may result in incorrect operation. This occurs on an internal B-C-to ground fault when the positive-sequence current all comes from one end of the line and the zero-sequence current all comes from the opposite end. In addition the magnitudes must be exactly right, and the phase angle of the positive- and zero-sequence system impedances must be equal. This same condition exists for the pilot-wire relay which uses the same type of network filter, but in no installation has this exact condition ever been encountered. If it is a condition which must be satisfied, the use of positive sequence alone is a solution.

The Equipment

The relay proper contains the positive plus-zero sequence filter, the operating element, and the fault detector. Figure 5 is a picture of the relay in a case equipped with switches to facilitate testing. The trigger circuit, relay tube, and associated components are mounted on a 19-inch wide panel for relay rack mounting in the same cabinet as the carrier transmitter and receiver. Provision is made to check the operation of the circuits without disturbing the installation.

Field Tests

In order to obtain field experience under typical service conditions, the first installation of this carrier relaying system was made in April 1944 on a 120-kv transmission line of The Detroit Edison Company's system. Before being installed in the field, the equipment was set up completely by the company in its laboratory for a preliminary trial. A miniature transmission line, which could be fed independently from either end, was connected to the relays through current transformers, and all types of fault conditions were simulated. These preliminary tests did not disclose any incorrect relay operations during a large number and variety of applied faults. The tests also permitted a very complete study of the effects of all adjustments on the equipment.

After this thorough trial in the laboratory, the equipment was installed on the Trenton Channel–Warren line 2 (see Figure 6) which is one of four parallel circuits feeding the Warren station from the

Trenton Channel powerhouse. On April 2, a series of staged tests was run, in which artificial faults were applied to the line. Figure 6 shows the pertinent details of the line and bus arrangement, together with the special test connections used. To minimize the disturbance to the system, two circuits on the same tower line were connected in series, as indicated on the diagram, and energized at only one fifth of normal voltage from the Warren station 24-kv bus. To compensate for the lower current resulting from this circuit arrangement and to obtain values more representative of actual faults, the secondary current was stepped up three times through a set of auxiliary current transformers before feeding into the relay. The 24-kv system is solidly grounded in the same way as the 120-kv system. The a-c source was connected to the line 1, and the faults were applied to line 2 at the Warren station terminal under three different test conditions. In the first condition, the fault was applied at point *A* (Figure 6) just external to the protected zone. For the second condition, the fault was applied at the same point, but at Warren station the polarity of the sequence-filter output was reversed to simulate an internal fault with a source of power at both ends of the line. For the third condition, the fault was applied at point *C* to simulate an internal fault fed from only one end of the line.

Ten tests were made under each of the conditions outlined, including a solid three-phase fault and all combinations of line-to-line, double-line-to-line-to-ground, and single-line-to-ground faults. In all cases, the test condition was set up with the 120-kv lines de-energized and then the 24-kv breaker was closed to initiate the fault current. In every case, the relays operated correctly. No tripping occurred on any of the external faults, whereas all internal faults were cleared promptly by the action of the proper circuit breakers.

Oscillograms were taken at Warren station of every test and showed essentially consistent and positive operation of all components of the relaying system. The test control sequence was such that oscillographic records of the beginning and the end of the fault-current flow were recorded whether the fault was cleared by the line breaker or by the test feed breaker. This was done so as to include on the oscillograms the effect of switching transients.

The average relaying time was 2.5 cycles with the slowest operation a cycle longer which was probably due to the random initiation of the fault in terms of the 60-cycle wave. This can result in a variation of about one cycle in the point at which the trigger circuit applies operating voltage to the grid of the relay tube. For these tests, the plate circuits of the thyratrons *V*1 and *V*2 were continuously energized, and the fault-detector contact was in the plate-supply lead to the relay tube.

Typical oscillograms taken during the staged tests are shown in Figures 7 and 8. The quantities recorded include relay current in the three phases and neutral, detector plate current which indicates received carrier and restraining voltage, operating voltage applied to the grid of the relay tube, the plate current of the relay tube, oscillator plate current, trip current, and fault-detector operation. It was felt that these quantities would give a complete record of the performance of the new system.

Figure 7 is an oscillogram of an internal fault. The fault detector operated in 0.75 cycle, and the operating element *R* closed to complete the trip circuit two cycles later. A slight time delay has been added intentionally to the relay element *R*. This was done as a precaution against operation on a single pulse of energy caused by a transient during the clearing of an external fault. The tests indicate that the time delay selected was more than enough. Additional tests in the laboratory are being made to determine the amount of time delay, if any, which will be required. Figure 8, which is an oscillogram of an external fault, indicates that the time delay is unnecessary. Reference to the trace of relay-tube plate current in Figure 8 shows that it remained zero during the entire time of fault. The external fault was cleared by automatically opening the 24-kv breaker approximately 18 cycles after it closed. The wave form of the quantities recorded on the two oscillograms is in good agreement with the theoretical values shown in Figure 3. The carrier receiver was adjusted for a high sensitivity to obtain the maximum effect of any transient which might affect the received carrier signal.

The equipment was put into service immediately after the tests, and has performed according to expectations to the date of this writing. Special records on the operation of this equipment are being kept. Indicators and counters provide records of the operation of the fault-detector and the tripping relay. Warren station has an automatic oscillograph in service which records three-phase voltage and neutral current on the 24-kv system. Another has been added for this trial installation which records 120-kv phase and residual currents in the generator and the Trenton Channel–Warren line 2, and the received carrier signal. The operation of the fault-detector relay initiates both of these oscillographs, and it is expected that further valuable information will be obtained from this combination.

Conclusions

This new carrier relaying system, by combining the advantages of electronic tubes and symmetrical-component filters, results in a marked simplification over previous carrier relaying systems. The number of relays required for complete phase and ground protection has been reduced to one per terminal, and the number of moving elements in the relay has been reduced to two.

The system operates on current only, eliminating the need of a source of potential and the attendant requirement of minimum voltage operation. It will not trip during system swings or out-of-step conditions. It is particularly well adapted to installation on circuits where existing relays would provide satisfactory backup protection to a carrier system. For this reason, lower-voltage circuits from 66-kv down appear to present a large number of possible applications for the new relay system.

The use of a fault detector retains the feature of an intermittent carrier system and permits the use of the relaying carrier channel for other services.

The system compares phase positions of current and therefore can be made very sensitive. It is easily applied, and the saturation characteristics make it practically independent of fault-current magnitudes.

References

1. A Single-Element Differential Pilot-Wire Relay System, E. L. Harder, M. A. Bostwick. *Electric Journal*, November 1938.

2. Symmetrical Components, C. F. Wagner, R. D. Evans. McGraw-Hill Publishing Company, Inc., New York, N. Y., 1933.

3. A Versatile Power-Line Carrier System, H. W. Lensner, J. B Singel. AIEE Transactions, volume 63, 1944, March section, pages 129–33.

4. Application of Carrier to Power Lines, F. M. Rives. AIEE Transactions, volume 62, 1943, pages 835–44.

5. Protection of Pilot-Wire Circuits, E. L. Harder, M. A. Bostwick. AIEE Transactions, volume 61, 1942, September section, pages 645–51.

6. Relay Protection of Tapped Transmission Lines, M. A. Bostwick, E. L. Harder. AIEE Transactions, volume 62, 1943, October section, pages 645–50.

7. Fault Current Polarities Compared by Carrier, J. T. Logan. *Electrical World*, April 15, 1944, pages 84–5.

Current-Transformer Output and Application Charts

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Synopsis: The performance of a multiratio current transformer is most simply represented as a function among the admittance vector locus of the core for one turn, the secondary-turn number N, and the secondary impedance Z. If the ratio of N^2/Z is used as a variable, extremely simple application charts for ratio correction factor in relation to primary currents result. The ratio N^2/Z furthermore is basic in the matching of transformers and burdens for maximum volt-ampere output at given primary current. Methods for computing the optimum volt-ampere output are given, and a chart is derived from which all data for a correct matching can be obtained at once. The usefulness of these methods is explained in connection with various examples such as applied to relaying circuits commonly used.

IN recent years the theory of operating of multiratio bushing-type current transformers has been given extended study resulting in a simplification of the application curves.[1,2] There is still lacking, however, a single chart from which the ratio correction factor for any value of primary current, any turn number, any secondary burden, and burden power factor can be read directly. In this paper a study is made to determine the feasibility of such a chart.

To date, there is very little information available on the secondary-output variation with relation to primary current, turn numbers, and burden. In many instances the secondary volt-ampere output is the quantity determining the operation of connected apparatus, such as instruments and relays. It therefore appears that benefits would be derived from a formulation of the output characteristics, particularly in the field of low primary currents. Of particular value would be the development of a volt-ampere-output application curve which would enable the relay engineer to determine quickly the optimum performance.

Standard Application Curves

In recent years numerous efforts have been made to simplify the representation of errors in multiratio relaying-type current transformers. A particularly simple graphical solution results for the ratio correction factor and phase-angle error when the admittance-vector locus of the steel core is used.[1] The complex ratio can be represented as a function of the number of secondary turns, the secondary burden and the admittance vector for one turn.

If one designates

N = number of secondary turns
$\boldsymbol{Z}$ = total secondary burden
$\boldsymbol{Y}_1$ = admittance vector for one turn
$\boldsymbol{R}(1/1)$ = one-to-one ratio

the following relation exists:

$$-\boldsymbol{R}(1/1)=\frac{\frac{N^2}{\boldsymbol{Z}}+\boldsymbol{Y}_1}{\frac{N^2}{\boldsymbol{Z}}}$$

resulting in the transformer chart shown in Figure 1 for the case of a typical 23-kv specimen. All the following charts will be based on this 23-kv specimen. In most relaying applications knowledge of the phase angle is not required. The small amount of drafting work necessary in conjunction with Figure 1 can be avoided by plotting curves representing various N^2/Z values in a co-ordinate system with ratio correction factor as ordinate and primary current as abscissa.

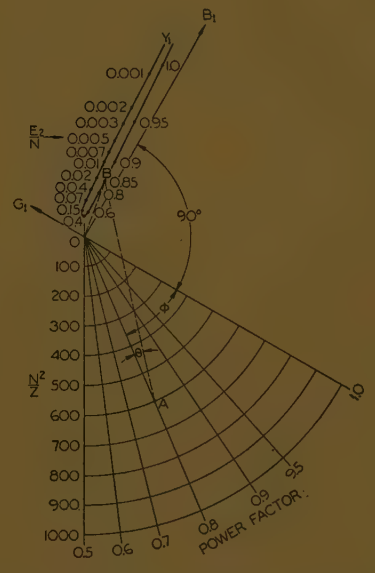


Figure 1. Chart for calculation of ratio and phase angle for any number of secondary turns N, any total burden Z at any power factor, and any desired secondary current I_2, provided $N^2/Z \leq 1{,}000$

Instructions:

1. Compute volts per turn I_2Z/N and mark point on locus Y_1, corresponding to this value as B
2. Compute N^2/Z and mark corresponding point A at proper power factor
3. Phase angle θ = angle OAB
   True ratio $R_N = I_1/I_2 = N \times AB/AO$

N = Number of secondary turns
Z = Total secondary impedance

Figure 2 represents a practical chart giving the required information without necessitating drafting and with the least amount of computations. As has been pointed out in a recent paper,[2] for most transformers the ratio error is not affected greatly by changes in the power factor of the burden. The curve therefore applies for any burden power factor encountered in practice. The use of the chart shall be explained in conjunction with an example:

Assumed total burden = 4.0 ohms (at 50 per cent power factor)
Transformer ratio = 250/5 = 50/1

What is the ratio correction factor for a primary current of 150 amperes?

Calculate

1. $\dfrac{N^2}{Z}=\dfrac{50^2}{4}=625$

2. $I_1\dfrac{Z}{N^2}=\dfrac{150}{625}=0.24$

Select this latter abscissa on the chart, point A, and follow vertically to the curve corresponding to $N^2/Z=625$. If there is no curve drawn for this value, interpolate between two curves including this value, giving point B. The ordinate of point B corresponds to the ratio correction factor (RCF), the value of which can be read on the ordinate axis, point C, giving RCF = 1.11. This chart permits the quick determination of the RCF of a specific transformer for any secondary-turn number, any secondary burden of any usual power factor and at any primary current. It has the advantage of not being limited to the specific burdens usually referred to on similar charts. This seems to be an important feature since burdens encountered in practice never or only accidentally correspond to these specific burdens.

Figure 2, together with the admittance-vector-locus chart shown in Figure 3, appears to satisfy the American Standard C57.1—4.033, covering current-trans-

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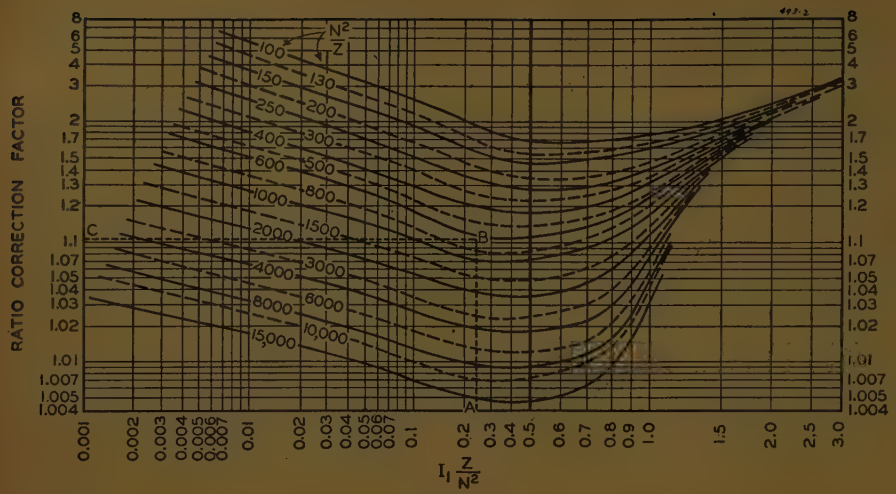


Figure 2. Chart giving ratio-correction factor for any number of secondary turns N and any total secondary impedance Z at power factors between 50 and 100 per cent and for any primary current

Internal $R = 0.0022N$ ohm
Internal X considered negligible

former application data completely and in the simplest and still most general form. A chart similar to Figure 2 can be prepared for the representation of the phase-angle errors. Such a chart, however, is sensitive to power-factor changes in the burden and therefore is limited to one specific power factor.

Secondary Volt-Ampere Output

It is the prime purpose of a current transformer to provide an appropriate supply of secondary power for the operation of various devices, such as meters, relays, and trip coils. These devices require a definite amount of power to perform the desired operation. Difficulties are encountered in many cases where operation is required at very low primary current. It is known that for each multiratio transformer there appears to be a definite ratio giving best performance under given primary current and load conditions. In the following it will be shown, that the optimum performance of any multiratio transformer can be determined from a turn-impedance matching chart requiring but a few simple computations. Since this chart is based on an analysis of the secondary volt-ampere output in relation to primary current, turn number, and secondary burden, these factors shall be investigated in detail. It will be seen that the admittance-vector locus offers a simple means of obtaining the desired data.

Variation in Volt-Ampere Output With Varying Secondary-Turn Number, Constant Secondary Impedance, and Constant Primary Current

Figure 4 shows the variation in secondary volt-ampere output for 500-ampere primary current and two secondary burdens of 3.4 ohms and 13.6 ohms, in relation to the secondary-turn number. It can be seen that pronounced maxima exist at 40 and 80 turns respectively. For the 3.4-ohm burden the output at 20 turns is only about one-third the maximum output of 325 volt-amperes occurring at 40 turns. It can be noted further that the maximum volt-ampere output is equal for the two burdens considered and that the following relation exists between optimum turn numbers and the respective impedances:

$$\frac{N^2}{Z} = \frac{40^2}{3.4} = \frac{80^2}{13.6} = 471 = \text{constant}$$

Matching of the impedance with the secondary-turn number is essential in order to obtain best performance. As an example, assume that the impedance $Z = 3.4$ ohms represents the trip coil of a circuit breaker which is directly connected to one of the secondary taps of the transformer whose primary current is 500 amperes. If the trip coil requires 300 volt-amperes to trip, it can be seen that tripping will be accomplished easily with the 40-turn connection but is impossible with any of the other standard connections of the secondary winding.

Variation in Volt-Ampere Output With Varying Secondary Impedance, Constant Turn Number, and Constant Primary Current

Figure 5 shows the variations in volt-ampere output for 500-ampere primary current, for various fixed secondary-turn numbers and varying burden impedance. The maximum volt-ampere output again amounts to 325 volt-amperes. The output is very sensitive to changes in magnitude of the burden impedance but is only little affected by changes in power factor as shown in Figure 5. The optimum performance occurs at the same N^2/Z value of 471 as found in Figure 4. From Figures 4 and 5 it follows that for a given primary current there exists a definite volt-ampere-output maximum. It can be seen also that independent of the secondary-turn number N the maximum occurs at a definite flux density of the core corresponding to $E_2/N = 0.83$ volt per turn in case of 500 amperes primary current.

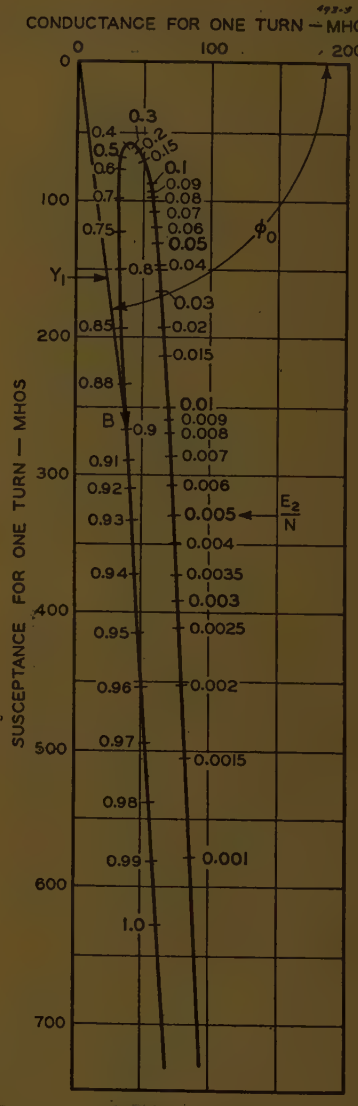


Figure 3. Admittance-vector locus Y_1 in mhos for one turn as a function of volts per turn

Determination of exciting current:

1. Determine volts per turn I_2Z/N giving point B, $Y_1 = OB$ and ϕ_0
2. $Y_N = Y_1/N^2$
3. $I_0 = Y_N E_2$

Internal reactance considered negligible
Internal resistance, $R = 0.0022N$

Variation in Volt-Ampere Output With Varying Primary Current

Increased volt-ampere output requires increased primary current. Figure 6 represents tests on 80 turns with varying primary current and varying secondary impedance. At 1,000 amperes the maximum equals 710 volt-amperes; at 700 amperes 480 volt-amperes; at 500 amperes 325 volt-amperes, and at 300 amperes 175 volt-amperes.

For various primary currents and constant number of turns the maxima do not occur at the same secondary-burden impedances and hence not at the same value of N^2/Z. Therefore, if a transformer has been matched for optimum volt-ampere output at a specific primary current, the secondary impedance and turn numbers must be rematched for another primary current to produce maximum output. The curves shown in Figures 4, 5, and 6 are too involved for use as practical application curves. Figure 7 shows a curve A which relates the primary current with the burden impedance Z and the turn number N, in particular N^2/Z, at which the output maximum occurs. Curve B designates the maximum volt-ampere-output corresponding to various primary-current values. The use of this chart will be explained by means of a specific example.

A relay having a burden of six ohms is set to operate at five amperes. What is the minimum primary current value at which tripping takes place and what is the corresponding optimum turn number?

The secondary volt-amperes at tripping are equal to $5^2 \times 6 = 150$ volt-amperes. From curve B, Figure 7, the corresponding minimum primary current amounts to 275 amperes. If we refer to curve A, the corresponding N^2/Z value amounts to 260. Since Z is equal to six ohms

$N^2 = 6 \times 260 = 1{,}560$ giving

$N = \sqrt{1{,}560} = 39.5$ turns

The closest turn number available on standard taps is 40 turns. The relay should be connected to this tap; if the relay were connected to a different tap of the transformer, tripping would take place only at a higher primary current. It is apparent that impedance matching is essential in order to procure best performance.

Graphical Determination of Volt-Ampere Output From Admittance-Vector Locus

The secondary output can be determined readily from the admittance-vector locus, for one turn as will be explained in the following.

Figure 8a shows a graphical representation of the primary, secondary, and exciting ampere-turn relation of a transformer with equal number of primary and secondary turns N. For the case of one primary turn and N secondary turns the single-turn primary current can be obtained from the relation $I_1' = NI_1$.

The current diagram 8b is obtained from the ampere-turn diagram by dividing the vectors in diagram 8a by the turn number N. As a first step it will be indicated how the volt-ampere output can be constructed from this current diagram. This method requires an exciting-current curve in polar form.

For a fixed value of primary current the distance BC in Figure 8b remains constant, whereas $OB = I_e$ and $OC = I_2$ vary with the secondary impedance, respectively with corresponding variation in volts per turn. With open circuited secondary the primary current is equal to the exciting current $I_1 = I_{e0} = OA$. The procedure of obtaining the volt-ampere output for constant primary current is therefore to assume a secondary-voltage value E_2, determine the exciting current giving point B and find the intersection of the circle with center at B and radius I_1 with the line OC corresponding to the power-factor phase angle of the secondary burden, giving $OC = I_2$ and the output $= I_2E_2$.

However if the vectors in Figure 8b are multiplied by N^2/E_2, the admittance diagram 8c is obtained. Since the admittance-vector locus is available (Figure 3), it is advantageous to use it in the determination of the volt-ampere output. Whereas in Figure 8b, distance BC is constant, in Figure 8c it varies with the secondary volts per turn value E_2/N.

The secondary volt-ampere output for a constant primary current $I_1' = NI_1$ and a specific value of E_2/N represented by point B can be obtained as follows:

1. Calculate $\dfrac{I_1'}{E_2/N}$

2. Intersect the circle with center B and radius $I_1'/(E_2/N)$ with the power-factor line OD, producing point C.

3. The volt-ampere output at point B is equal to $E_2^2/Z = (E_2/N)^2 N/Z$ and is obtained by scaling OC and multiplying its value by $(E_2/N)^2$ of point B. This output is plotted most conveniently in an orthogonal co-ordinate system with OD as abscissa, as indicated in Figure 8c.

Figure 8c brings out the important fact that the volt-ampere output is not primarily a function of Z or N, but of the ratio N^2/Z. This is the basic variable in volt-ampere-output problems as well, and it is for this reason that the impedance matching curves (Figure 7) become simple.

Mathematical Determination of Volt-Ampere Output

Without loss in generality, the volt-ampere output can be computed by assuming a transformer having one primary and one secondary turn. In the equivalent diagram, it can be represented as shown in Figure 9 where Z_T represents the impedance of the transformer and Z_B the secondary-burden impedance. Depending on the value of Z_B, a larger or

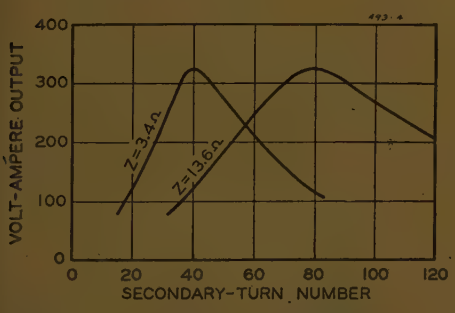


Figure 4. Variation in volt-ampere output with varying secondary-turn number, fixed secondary impedances, and constant primary current

$I_1 = 500$ amperes
Burden power factor = 0.5

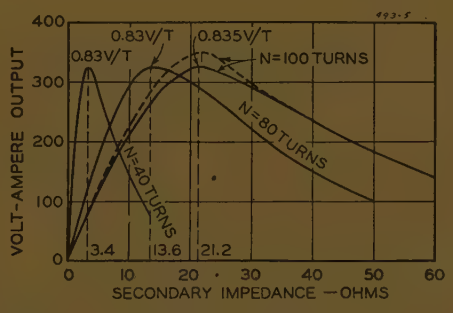


Figure 5. Variation in volt-ampere output with varying secondary impedance, fixed turn numbers, and constant primary current

$I_1 = 500$ amperes
——50 per cent power factor
– – –90 per cent power factor

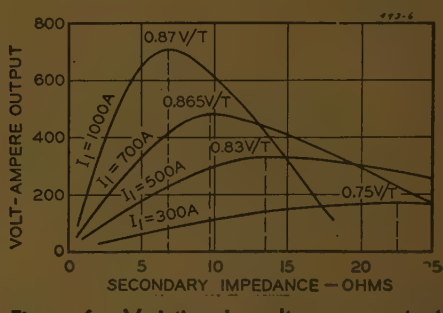


Figure 6. Variation in volt-ampere output with fixed primary currents, varying secondary impedance, and constant secondary-turn number

50 per cent power factor
$N = 80$ turns

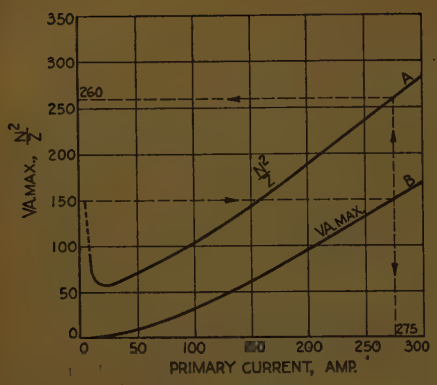


Figure 7. Impedance matching chart giving maximum volt-ampere output and corresponding N^2/Z for a given primary current

smaller portion of the constant total current I_1 is diverted through the secondary winding. Z_T is equal to $1/Y_1$ and is obtained from Y_1 by complex inversion, resulting in Figure 10. In case of a highly reactive burden Z_B, a close approximation is obtained by neglecting the resistance in Z_T and Z_B. This simplification is similar to that previously made in the case of inphase addition of currents.

It has been shown further that the susceptance component B_1 of Y_1 is proportional to the eighth power of the secondary voltage for high flux densities, and proportional to $E^{-0.4}$ for very low flux densities.[1]

An example covering the high flux-density range will be computed in the following, the primary current being assumed 500 amperes: With the transformer open-circuited, the entire primary current is used to magnetize the core, resulting in

$$I_1 = Y_{1,0}E_{2,0} \text{ for } N=1$$

Reference to Figure 3 proPuces $Y_{10}=514$ mhos, $E_{2,0}=0.973$ volt. These values can be established by a few trials.

Y_1 for any secondary voltage E can therefore be expressed as

$$Y_1 = 514\left(\frac{E}{0.973}\right)^8$$

and

$$Z_T = \frac{1}{Y_1} = \frac{1}{640E^8}$$

If we refer to Figure 9, the following relations exist:

$$(500-I_2)Z_T = E$$

$$(500-I_2)\frac{1}{640E^8} = E$$

Hence

$$I_2 = 500-640E^9$$

and

$$VA = I_2E = 500E-640E^{10}$$

which derivative with regards to E is to be zero for maximum volt-ampere output.

This results in

$$E_m = \sqrt[9]{\frac{500}{6{,}400}} = 0.754 \text{ volt}$$

Hence

$I_2 = 450$ amperes

and the maximum output $= 339\,VA$

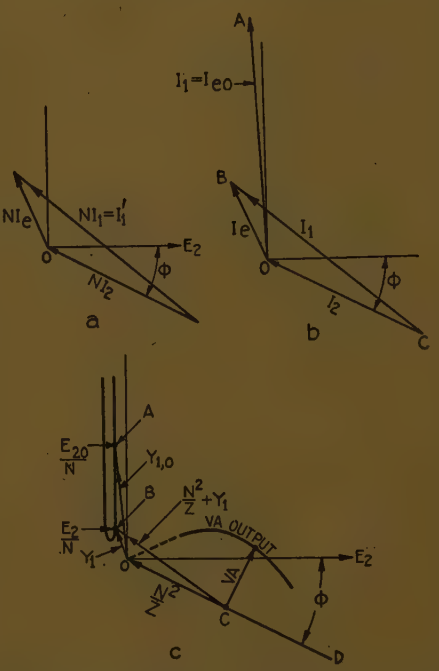


Figure 8. Graphical representation of relations between primary, secondary, and exciting ampere turns, currents, and admittances

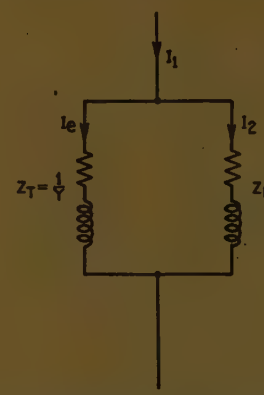


Figure 9. Equivalent circuit for bushing-type current transformer

This result checks closely with the values obtained in Figure 6.

Relaying Circuits

In many of the most common relaying circuits there is usually a large safety factor between the minimum amperes required to actuate the relay and the actual short-circuit current. In these cases little attention has to be paid to impedance matching in order to produce maximum volt-ampere output. In special cases, however, where the short-circuit current is low, where part of the secondary current of the transformer of the short-circuited phase is by-passed through idle transformers, and finally where impedances of relays are high or vary within great limits, a correct matching of the impedances becomes important.

In previous example it has been shown how the combination of a single transformer with a relay can be tuned to produce tripping at minimum primary current. The method outlined determines directly the optimum turn number for a given relay impedance by use of Figure 7. The method can be applied directly for a symmetrical three-phase short-circuit in conjunction with three-phase relays and one ground relay in accordance with Figure 11a. In case of single-phase-to-ground and phase-to-phase faults the method has to be modified because of the current by-passed in the idle transformers of the normal phases. For approximation calculations some simplifying assumptions can be made. The load current in the two normal phases will be neglected, and, whenever permissible, inphase addition of currents shall be applied. Small impedances also can be neglected when in series with large ones. Because of the nonlinearity of the circuits caused by the variation in the impedances in the by-passed transformer as indicated in Figure 10, the optimum performance has to be determined by a step-by-step method. Since no general rules applying to all the numerous relaying circuits can be set up, the application of the methods shall be given in conjunction with a specific example.

In the circuit shown in Figure 11a a

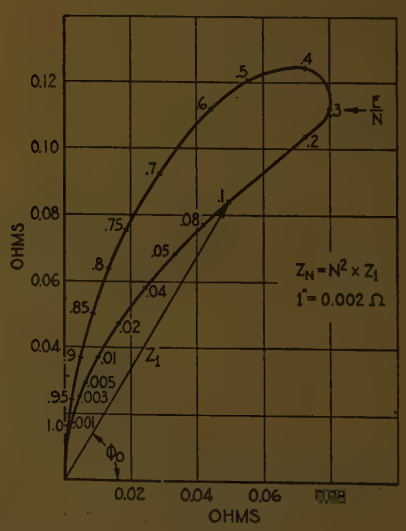


Figure 10. Impedance-vector locus Z_1 for one turn as a function of volts per turn

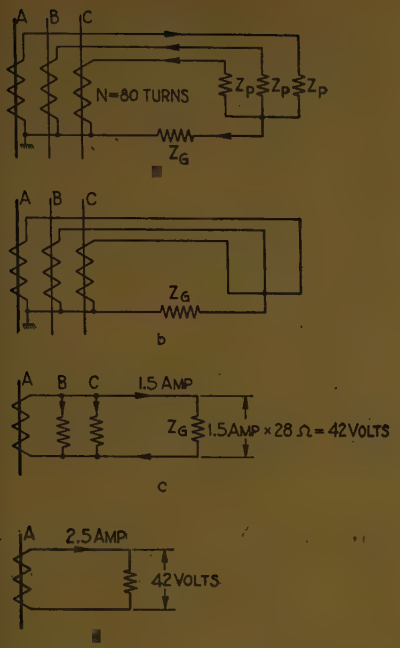


Figure 11. Relaying circuit and simplified representation

combination of ground relays has an impedance of 28 ohms (700 volt-amperes at five amperes) at 60 per cent power factor. The ground relays are actuated at 1.5 amperes.

The phase relays have an impedance of 0.2 ohm (five volt-amperes at five amperes) and 70 per cent power factor. A single-phase-to-ground fault is assumed in phase A.

The problem consists in determining:

1. The minimum phase-to-ground current in phase A necessary to trip the ground relays.

2. The corresponding optimum secondary-turn number N of the current transformers.

In order to calculate the current by-passed by the idle transformers B and C, the small impedance of the phase relays can be neglected, and the circuit can be simplified according to Figure 11b. Since no rigorous solution is possible, the minimum primary current (which is just necessary to trip the ground relay) corresponding to several transformer ratios has to be determined, the results of which have been plotted in Figure 12. From these values the optimum turn number

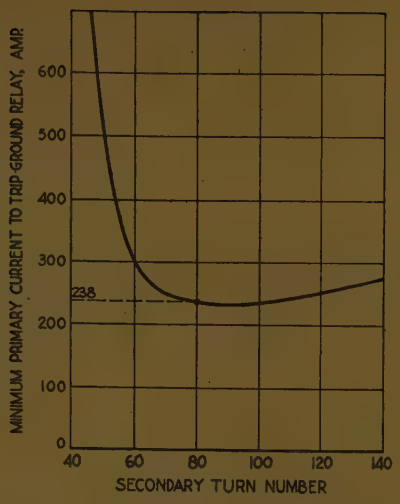


Figure 12. Optimum number of turns for relaying circuit of Figure 11

can be determined. As a first attempt the 400/5 ratio is selected corresponding to 80 turns. As a first step the currents by-passed through the secondary circuits B and C have to be determined. The admittance of the current transformers B and C can be obtained from Figure 3. If we refer to the simplified Figure 11c and recall that the ground relay trips at 1.5 amperes, the corresponding volts per turn are $42/80=0.525$. The admittance Y_1 is 77 mhos for one single turn. For 80 turns

$$Y_{80}=\frac{Y_1}{80^2}=\frac{77}{6{,}400}=0.012 \text{ mho}$$

The circuit can be simplified further by combining the impedance of the secondary windings B and C with that of the ground relay. Since the power factors of relays and current transformers are not very different, the resultant admittance can be obtained by numerical addition.

$$Y=Y_B+Y_C+Y_{\text{relays}}=2\times 0.012+0.0357=0.0597 \text{ mho}$$

corresponding to a resulting impedance of 16.8 ohms. The total secondary current necessary to drive 1.5 amperes through the circuit is therefore

$I_2=42/16.8=2.50$ amperes.

The problem now resolves itself to determine the primary current necessary to produce 2.50 amperes across a secondary burden of 16.8 ohms at 60 per cent power factor (see Figure 11d). This can be done by means of Figure 1.

The volts per turn are $\frac{42}{80}=0.525$

$$\frac{N^2}{Z}=\frac{6{,}400}{16.8}=381$$

$$R(1/1)=\frac{AB}{AO}=1.19$$

$$R_{80}=I_1/I_2=80\times 1.19=95.3$$

Therefore

$I_1=95.3\times 2.50=238$ amperes

If 80 turns are used, tripping can be accomplished for a relay current of 1.5 amperes with a primary current of 238 amperes. By repeating the calculations for other numbers of turns, a curve as shown in Figure 12 is obtained. From this figure it can be seen that the optimum performance occurs for $N=90$ turns.

Conclusions

A simple application chart from which the ratio correction factor can be determined for any value of primary current, any number of secondary turns, and any secondary burden of practical power factor has been developed.

The secondary volt-ampere output of a current transformer at a given primary current varies with the secondary-turn number and secondary burden. In order to obtain maximum volt-amperes at a given primary current, matching of the secondary impedance and turns is required. A volt-ampere-output application curve has been developed which is readily applicable to numerous relaying problems.

References

1. Current-Transformer Performance Based on Admittance-Vector Locus, A. C. Schwager. AIEE Transactions, volume 61, 1942, January section, pages 26–30.

2. Bushing-Type Current-Transformer Characteristics, S. D. Moreton. AIEE Transactions, volume 62, 1943, September section, pages 581–5.

Economics of Trolley-Coach Operation

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MEMBER AIEE

Synopsis: The trolley coach is one of the newer vehicles used in urban transportation. In 1943 trolley coaches carried 8.7 per cent of all passengers on the transit lines of all United States cities having a population between 50,000 and 1,000,000, although trolley coaches are only 6.6 per cent of the vehicles. This paper points out the features of trolley-coach operation which fit it for a wide variety of service conditions. Results of trolley-coach operation in increasing gross revenues are given. The various developments which have contributed to low transportation expense are outlined. Comparisons of trolley-coach, streetcar, and motorbus operating expenses are included. Further progress in trolley-coach design, which can be expected to follow the war, will enhance the trolley coach's economic position.

SINCE the introduction of the modern trolley coach in 1928, it has demonstrated in almost every installation its ability to increase revenues and to operate at very low cost. The investment cost for trolley-coach service is low, and the quality of that service ranks very high compared to other transportation media. The vehicle itself and the materials used in the distribution system have been improved continually with resultant improvement in the financial showing. Further progress is under way.

As a result of the success of early installations, there are 3,500 trolley coaches in service in the United States and Canada. The number has been reached in spite of the years of depression and war, both of which have retarded the growth.

In the broad field of urban transportation there is room for many types of public conveyances; rapid-transit trains, streetcars, trolley coaches, motorbusses, and taxicabs. Rapid-transit elevated or subway service is limited to a few large cities, and taxicabs furnish a distinct type of service at a much higher price; therefore both will be omitted from further discussion. But most transit companies, in cities of 100,000 population up, are faced with the possible use of the three remaining types of vehicles: In order to develop the most useful transportation system for any city, it is necessary to answer the question, "Which vehicle or combination of vehicles will afford the best service to the public at a reasonable cost?"

The problem is almost always complicated by the presence of some form of public transportation that is inadequate or otherwise unsuitable. Sometimes it is a streetcar system whose only excuse for existence was the hope of its promoter that he might make an honest dollar. Sometimes it is a motorbus system which grew from unemployment and easy credit in a community which now would be better and more economically served by trolley coach or streetcar.

Although the transportation engineer cannot hope to find virgin territory where each type of vehicle can be fitted into its correct economic niche, the factors determining the selection of the proper vehicle should be recognized. Once the right perspective is obtained, the economic chaos which characterizes too many of our activities can be avoided. Investment, earning power, and cost of operation are the three important factors in determining the correct application.

Investment

The first consideration is the initial cost of an installation. It can be assumed that for equal standards of convenience and excellence shops, car houses, garages, and similar items will be about the same, regardless of the type of vehicle. Of course, the detail arrangements differ, and each form of vehicle requires a maintenance facility or practice peculiar to itself: such as tracks for the streetcar, double overhead wires for the trolley coach, and fueling provisions for the motorbus. Likewise some feature which keeps down the first cost may entail considerable differences in day-to-day expenditures for the several forms of vehicle. For example, with outdoor storage in cold climates electric vehicles may require preheating in varying amounts proportional to their size, and motorbusses need engine idling to prevent freezing and insure starting in the morning. Such things must be evaluated in determining the initial investment for maintenance facilities.

The modern streetcar has the highest first cost, some $18,000. The trolley coach is next at $13,000. The motorbus is the cheapest at $12,000 for a vehicle comparable to the trolley coach and ranging from that value downward, depending on how far one wishes to depart from that level. The streetcar has approximately 58 seats compared to 40 to 44 for the largest trolley coaches and motorbusses in common use; therefore fewer streetcars are required to handle equal traffic. The trolley coach is capable of higher schedule speeds than the motorbus; hence fewer trolley coaches are required for a given frequency of service. Also the electrically propelled vehicles have greater availability, and hence fewer spares are necessary.

The investment required for streetcar tracks is very high, $100,000 per mile of double track in paved street being a normal figure. As a result there has been little completely new track built in many years. When new track is required on a route, the advisability of replacing it or substituting some form of rubber-tired service must be studied. The trolley coach requires overhead lines which may cost from $7,000 to $20,000 per route-mile (four wires) depending on the conditions and the degree of permanence desired. The routing of trolley-coach lines warrants the most serious study. Not only is it wise to analyze shifts in population and travel habits to forecast their trends, but it is doubly desirable to lay out routes with as few crossings and turnouts as possible. Not only does complicated overhead greatly increase the first cost, but it slows down operation and increases maintenance cost.

However, first cost does not tell the whole story. It is apparent that the operating expenses of streetcars must be very much lower than those of motorbusses to offset their greater fixed charges. The fixed charges of the trolley coach obviously fall between the two, being materially less than those of the streetcar with its tracks, but still somewhat greater than those of the motorbus which uses no overhead. On the other hand, the operating expense of the trolley coach is so low that there is a very wide field where its total annual costs are lower than those of either streetcar or motorbus.

Gross Revenue

One of the features in which the trolley coach excels is its ability to attract and retain new patronage. The reasons for this passenger appeal are evident. The trolley coach is cushioned on rubber tires. It is not tied to rails like the streetcar,

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and therefore it is not subject to the annoying delays caused by broken-down or improperly parked automobiles and trucks. It loads at the curb. On the other hand it affords a higher type of transportation service than the motorbus. Its light and heat are obtained from central-station power; hence, both are ample. Adequate ventilation is provided. The trolley coach is free from the odors of hot oil and exhaust gases. Its accelerating rate is high, but there are no jerks due to interruption of torque during gear shifting as on motorbusses. Electric and air braking combine to give maximum safety and reliability The trolley coach

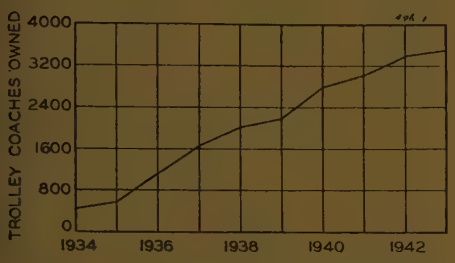


Figure 1. Ten-year record of trolley coaches owned in the United States

is the quietest public transportation vehicle.

The result of these superior characteristics is increased patronage. In one West Coast city the number of passengers on one route increased 15 per cent and on another 20 per cent when trolley coaches were substituted for streetcars. In an industrial city in Ohio where several lines were converted from motorbus and streetcar operation to trolley coaches, the revenue per vehicle-mile increased 20 per cent, and operating costs per vehicle-mile decreased 14 per cent. Gross revenue on one line in a large eastern city increased 47 per cent, although the system earnings during the same period decreased. On another line in the same city the increase in gross revenue was 21 per cent. In one southern city a new trolley-coach installation had a gross revenue increase of 47 per cent compared to an increase of 10 per cent on representative rail lines. In one eastern city 18 rail lines were converted to trolley-coach operation over a ten-year period. In every case patronage and revenue were increased.

Operating Expenses

Not only are trolley coaches able to increase gross revenues, but they are very economical in operation. Trolley coaches have been built in sizes seating 30 to 56 passengers, but most coaches have been of 40- to 44-passenger capacity. The discussion of operating expenses will be limited to that size.

Way and Structures

The maintenance of way and structures is now approximately 0.8 cent per coach-mile, compared to two to four times that amount per street-car-mile and 0.1 to 0.3 cent per bus-mile. The principal expenditure is for overhead lines, and great progress has been made in line construction as well as in current collectors. In the line material all parts attached to span or guy wires such as frogs and crossings are substantial castings to which are bolted renewable runners. These wearing parts can be renewed easily and quickly without disturbing the original construction. High-speed rigid curve segments with continuous wire underrun greatly reduce the complexity of the system of span and guy wires. These devices result in lower maintenance.

Another source of maintenance expense has been the gouging of the trolley wire adjacent to "special work" by the collectors. Proper design of frogs and crossings to avoid tilting of the collector not only reduces wire maintenance but also permits operation at higher speed.

The earlier trolley coaches used wheel trolleys similar to those used on streetcars, except that swivel harps allowed the coach to operate at either side of the overhead wires within the range of the trolley poles. The wheel trolleys caused considerable arcing and resultant short life of wire. They were noisy, and even with high contact pressure dewirements were too frequent. Wheel trolleys were soon superseded by bronze or steel shoes which overcame some of the difficulties. It was necessary to lubricate the wire for metallic-shoe operation. This was an expensive procedure, and excess lubricant was deposited on the coaches and the street.

The success of carbon inserts in locomotive pantograph trolleys led to experiments in the use of carbon insert shoes. A satisfactory renewable carbon insert shoe has now been developed and is used extensively. The low contact pressures, self-lubrication, and current-collecting characteristics of the carbon are resulting in great improvement in life of overhead construction. As a result of these devices which are now available and probable future progress in design, material reductions in operating costs may be anticipated.

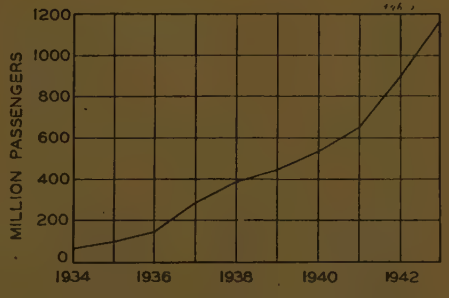


Figure 2. Ten-year record of transit passengers carried by trolley coaches in the United States

Maintenance of Equipment

Progress, which has been made in the vehicles themselves and the electric propulsion equipment, has effected economies in maintenance. The earlier 40-passenger coaches were propelled by two 65-horsepower motors. These were succeeded by a single 125-horsepower motor with a simplified control and with a double-reduction spur and bevel gear (or spur and hypoid) axle which proved to be superior in service to the worm-drive axles previously used.

Early trolley coaches had two principal items of maintenance: current collection and brakes. The former has been discussed. In the early days brake drums and linings were the same as those used on motorbusses. The trolley coach operates at higher speeds, and as a result there are more brake applications per hour, and those applications are made at higher vehicle speeds. Also the braking effect of an engine is absent. Life of brake lining and brake drums was very short. The single-motor axle provided more adequate braking equipment, but further improvement was desired.

This requirement was met by the introduction in 1938 of 140-horsepower motors with dynamic-braking control. A relatively small reduction in temperature of brake linings gives a relatively large increase in lining life. Standard practice is to provide an electric-braking rate of 2.0 miles per hour per second, whereas the total service braking rate is 3.0 to 4.0. This reduction of duty on the mechanical brakes has been sufficient to increase brake-lining life to five or six times the life obtained without electric braking. The life of the brake drums should be as long as the coach life. Reduced mechanical braking reduces tire temperature and thereby increases the life of the tires. More than 97 per cent of the trolley coaches purchased since 1938 are of the electric-braking type.

Trolley-coach maintenance in recent years has been approximately 2.70 cents per coach-mile, 0.90 cent of which is for tires and tubes. This amount should decrease as the mileage of modern vehicles expands. Modern streetcar maintenance

is 1.7 to 2.0 cents per mile. The maintenance of motorbusses of comparable size averages 4.0 to 5.0 cents per mile.

Power

The amount of power required for trolley coaches varies widely, depending on the conditions of operation. Greater weight, higher schedule speeds, hilly routes, and similar conditions increase energy consumption. The average energy consumption of 40-passenger trolley coaches according to recent American Transit Association data is 3.8 kilowatt-hours direct current per coach-mile. A modern streetcar requires approximately 5.4 kilowatt-hours per car-mile. The average cost of power is close to 1.0 cent per kilowatt-hour direct current.

The average cost of fuel and lubricants for 40-passenger motorbusses under similar conditions is approximately 4.0 cents per coach-mile, excluding gasoline tax which may be another 2.0 cents per coach-mile. Lubrication is a negligibly small item on trolley coaches but on motorbusses it is ten per cent or more of the fuel cost.

The central-station industry has a long record of decreasing the unit cost of power. Not only is the present power cost for the trolley coach favorable, but the prospects are that it actually will decrease. On the other hand, the future price of oil and gasoline is almost sure to increase. There is disagreement among the experts as to the adequacy of our supply of oil, even if we change our wasteful methods of use. One cannot expect any reduction in taxation of liquid fuels but should be prepared for increases.

Conducting Transportation

The principal item in public transportation expense is the wages of vehicle operators. At the present time wages per hour vary widely in different localities and among different types of vehicles. However, the cost of wages per vehicle-mile is inversely proportional to the schedule speed. The practically unlimited supply of power and the uninterrupted acceleration of the trolley coach give it marked advantage. It is true that some abnormally high-powered motorbusses may equal trolley-coach schedules, especially on brief test runs. However, in the same or identical service the trolley coach maintains schedule speeds 10 to 15 per cent higher than the usual motorbus. The power of trolley-coach propulsion equipment does not deteriorate with age and is practically independent of variations in air temperature and barometric pressure. Also the trolley coach is so easy to operate that driver fatigue does not reach the point where it affects schedule speeds.

In the long run administrative and general expense should be essentially the same for a given system regardless of the type of vehicle. At the present time different systems of accounts, different conditions with respect to insurance, and other items cause wide variations.

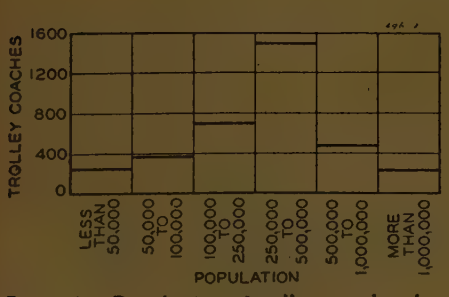


Figure 3. Distribution of trolley coaches by population groups

Table I

	Trolley Coach	Street-car	Motor-bus
Average seating capacity	44	52	29
Average passengers per one-way trip	31.8	38.4	17.9
Passengers per vehicle-hour	80	80.5	62
Operating cost per vehicle-hour less administrative and supervisory expense, per cent	100	141	107
Operating cost per passenger, per cent	100	140	138

Future Prospects

It is believed that the future of the trolley coach is particularly bright. New materials and new methods will result in lighter, more economical, and more attractive coaches. Also the motor bus has profited from the development of millions of private automobiles and tens of thousands of motorbusses. The modern streetcar is the result of an intensive development program costing hundreds of thousands of dollars. While the trolley coach has taken advantage of developments in both fields, it has never received the amount of study which the others have, and relatively greater progress may be expected in the future. Further progress in design of electric propulsion equipment may be confidently expected.

All electrical engineers are familiar with the advantages of separate ventilation for the efficient cooling of electric apparatus. Clean dry air results in improved motor commutator and brush performance especially where the air for self-ventilation has been full of dust and dirt. In the case of the trolley coach the motor losses stabilize the temperature of the air which is later blown over the resistors for coach heating. In many installations of dynamic-braking equipment, the resistor losses during acceleration and braking are frequently adequate for coach heating with only a small amount of supplemental heat from the line. In the colder climates a large proportion of the motor losses can be used for coach heating in the winter months. The fans designed for ventilation and running at constant speed require less energy than fans for motor self-ventilation which operate over the whole range of motor speed.

Table I shows an example of relative operating costs in a city having trolley-coach, streetcar, and motorbus service under generally similar conditions, except that the trolley coaches and motorbusses are modern. The streetcars are 20 years old. The motorbusses are of smaller capacity than the trolley coaches and operate under easier traffic conditions. When conditions permit, it is expected that trolley-coach service will supersede the streetcar service on all present carlines.

Figure 1 shows the number of trolley coaches owned in the United States by years from 1934 to 1943. As of December 1, 1943, there were 3,502 trolley coaches.

Figure 2 shows the number of passengers carried by trolley coaches each year from 1934 to 1943. Trolley-coach passengers were 0.6 per cent of total transit passengers in 1934 and 5.3 per cent in 1943. In cities between 50,000 and 1,000,000 population the trolley-coach passengers were 8.7 per cent of the total in 1943.

The number of trolley coaches owned in cities of various population classifications is shown in Figure 3. Cities between 100,000 and 1,000,000 have 76 per cent of the total; those between 250,000 and 500,000, 43 per cent.

Conclusion

Generalizations are frequently misleading, because there is no substitute for detailed analytical study of any problem. However, a number of studies indicate that the proper economic field for the trolley coach is that where the interval between vehicles during base hours is not less than four or five minutes and not longer than 15 to 20 minutes. It is realized that both of these limits vary widely under different conditions and that other considerations such as political

Aircraft Fuses Must Protect

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Synopsis: The ever-increasing list of electric equipment needed for the satisfactory performance of the modern airplane has brought the electric system from a place of relative unimportance to one which cannot be ignored. The electric equipment must not fail, particularly on combat planes. Therefore, the protective devices must be designed and applied to permit the equipment to function as long as it can supply any useful work but to remove it from the circuit when it has ceased to function or has become a hazard. This type of electrical protection is obtainable with fuses of special design when applied properly.

THE evolution of the electric system in aircraft has paralleled the development of the airplane itself. The original six-volt d-c electric system was little more complicated than that employed in the automobile. As power requirements increased, the voltage was increased to 12, then to 27 volts, which was considered adequate for several years. Increased electrical demands have forced the voltage still higher to 120 volts direct current in some installations, and now a 208-volt three-phase 400-cycle system has ceased to be merely a dream but will become a reality in the not too distant future.

Unfortunately, the transition from one system to the next has been so gradual that the whole problem of circuit and equipment protection has not been reviewed to see if a completely new or unorthodox approach could effect better results. The techniques of the six-volt system more or less have been transplanted to the higher-voltage systems without reevaluating them in the light of the requirements of the new system. Fortunately the 208-volt three-phase 400-cycle system will not permit such a transition, so that now is an ideal time to analyze what is available and what has been used to obtain more complete electrical protection in aircraft.

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Fusing Requirements

The fusing requirements in aircraft differ little from the results desired in industry except that the requirements and demands of the former are much more stringent. To save weight the overload capacity of aircraft electric equipment is reduced to an absolute minimum, whereas in normal power applications extra weight is not so objectionable.



Figure 1. Time-delay fuse consisting of a heater, a thermally operable current interrupter, and a fast-acting short-circuiting member

Also, in aircraft the range of ambient temperatures to which a given installation is subjected is much greater than that found in normal power work. It is true that a power installation can be found where extremes in ambient temperatures may be experienced, but seldom, if ever, is one installation ever subjected to the range of −60 to +71 degrees centigrade as found in aircraft. Useless or unnecessary operation of protective devices in airplanes cannot be countenanced, particularly in military applications, whereas in most power applications objectionable operation of the protective device is seldom little more than a nuisance, hardly ever a catastrophe. Then the weight of the protection cannot be ignored in aircraft, thereby eliminating the use of many devices which have been proved satisfactory in industry. Hence, aircraft fusing can learn much from the techniques already employed in power work, but it must go further in accuracy to give the trouble free but complete protection required.

Electric fusing, whether it is applied to power work or aircraft, may be divided into two general categories:

1. Fuses to protect electric equipment.
2. Fuses to protect the circuit itself.

Consider first the practice now employed in power work. Since aircraft electric system voltages will not exceed 208 volts, the discussion can be limited to secondary power fusing.

Protection of Equipment

Electric equipment, with the exclusion of instruments and similar devices having practically no overload capacity, consists of a current-conducting path, usually metallic; insulation, either organic or inorganic; and a heat-absorbing mass, usually iron. The current-conducting path, insulation, and heat-absorbing mass are usually in good thermal relation, so that, under steady-state conditions, the heat generated in one member will elevate the temperature of all three components. Since the purpose of the electric equipment is to convert electric energy to mechanical, thermal, or chemical energy, and its efficiency is never 100 per cent, there always will be some electric energy converted to heat in the device so increasing its temperature.

When the equipment is subjected to slight overloads which are continued for appreciable times, failure will occur in the material which is damaged at the lowest temperature. Because there is time for heat conduction, the temperatures of the component parts of the equipment are elevated by the heat generated in the current-conducting path. This does not mean that there is not a temperature

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or financial affiliations sometimes dictate the course to be followed.

In the great majority of cities where trolley coaches have been tried on a reasonably large scale, their numbers have increased rapidly. Trolley coaches are the backbone of Seattle's transportation system, 292 being in use. Milwaukee has 274 coaches, Providence 204, Boston 162, Indianapolis 162, and Chicago 152. All of these latter cities started with relatively small initial installations and expanded them as a result of the favorable economic results. All of them contemplate further installations.

gradient in the device, but it does mean that this gradient remains unchanged if the load is maintained long enough to approach a stable condition. The point or temperature of failure, of course, is dependent upon the individual characteristics of the component parts of the device, but, once the various materials have been chosen, this point is established irrevocably. The overload may be the results of the malfunctioning of the electric equipment itself or may be due to a greater connected load than was anticipated in the design, but, in either case, the result is the same. In the final analysis this type failure may be regarded as a temperature failure even though it is caused by heat produced by electric energy. Hence, the condition is aggravated when operating in high ambient temperatures and alleviated in the lower ambient temperatures.

If the overload is increased, the length of time the equipment can withstand it is shortened, but, again, the mechanics of the failure is the elevation of the temperatures of the various component parts in their relative proportions until one fails. This process continues as the overloads are increased, the safe time steadily decreasing, but always dependent upon the thermal capacity of the whole device and influenced by the ambient conditions to which it is subjected. However, at some overload the heat generated in the current-conducting path will be so great that it will fuse before there is time to conduct some of the heat away from it to those components injured at a lower temperature. This type failure may be considered to be free from ambient effects, because the variation in ambient temperature, though theoretically affecting the failure in that less heat would be needed to cause failure at the higher ambient temperature, is so small a percentage that, for all practical purposes, its effect may be regarded as negligible. To illustrate the general theory, consider an electric motor. The current-conducting path is the copper wire; the insulation is enamel, rubber, glass, silk, or cotton used on the field windings; the heat-absorbing mass is the frame and rotor. On the lower overloads failure results from the insulation on the windings being destroyed, whereas on the higher overloads the copper in the winding itself actually fuses.

From this it is seen that the curve of the overload capacity or safe-operating characteristic of an electric device contains a point of inflection, because it is the combination of two curves having different slopes. The time–current curve of the copper is steep, whereas the curve in that region in which there is time for thermal conduction has a more gradual slope and is affected by ambient temperatures. A protective device, to give complete protection over the entire range without useless blows, must have the same characteristic. This characteristic cannot be produced by a single-element device, whether it be a fuse, circuit breaker, or thermal cutout. One answer to the problem is found in the time-delay fuse shown in Figure 1 time–current curve of which is shown in Figure 2. This device consists of a thermally operable current-interrupting device in series with a fast-operating short-circuiting member. Current flowing through the fuse generates heat in the heater as well as in the short-circuit strip. At the lower overloads most of the heat is supplied by the heater, but, as the overload is increased, the effect of the short-circuiting strip becomes more pronounced until finally, on the very high overloads where there is no time for thermal conduction, the fast-acting short-circuiting strip fuses before the thermally operable current interrupter can function. The time delay or time lag is obtained on the low overloads, because the heat generated in the heater must be conducted into the center member, called the absorber, and must elevate its temperature to the melting point of the fusible alloy holding the trigger before it can operate opening the circuit. The characteristics of this device are adjusted to approach the safe-operating characteristics of power electric devices, so that, if its rating is chosen correctly, complete electrical protection without useless blows is obtained. The melting point of the fusible alloy is chosen, so that the effect of ambient temperatures on the fuse is the same as on the electric device it is protecting. The time-delay fuse gives wonderful flexibility in design, because the heat-generating member, the fusible alloy, the mass of the absorber, and the characteristics of the short-circuiting strip can be adjusted independently giving any characteristic desired.

The United States Army Air Corps appreciated the potentialities of this type fuse and built their specification 94-32084 around it. Since, at the time the specification was written the highest system voltage in use was 27 volts, the short-circuiting characteristics were not specified. The specification covers the lower end of the curve by requiring the fuse to carry 110 per cent load indefinitely and to open at 125 per cent load within one hour and at 200 per cent load between 90 seconds and eight minutes. The omission of the unnecessary short-circuit requirements in the 94-32084 specification permitted a desirable saving in size and weight.

The fuse combining a fusible element with a thermally operable current interrupter is often condemned and precluded from aircraft applications, because it is felt that the increased protection is obtained only by increasing the weight which cannot be justified. Actually the use of such a fuse decreases instead of increases the weight of the protective device. This seeming anomaly is true, because the rating of the protective device can be decreased when time lag is built into it. To illustrate, consider a three-phase ten-horsepower 220-volt motor connected in series with ordinary fuses. Even though the full-load current of the motor is 27 amperes, 90-ampere fuses are used in the circuit to assure that the fuses do not blow under starting or transient conditions. Such a device only can furnish protection to the motor above 90 amperes. The fuse itself, neglecting the fuse block in which it is mounted, weighs 121 grams. If, instead of the conventional fuse, a fuse having a thermally operable circuit interrupter as well as a short-circuiting strip is used, the rating of this device can be reduced to 30 amperes, and still it has sufficient time lag to withstand starting and transient currents. With such a device the motor is protected over its entire range of overloads and short circuits by a device weighing only 20 grams. Therefore, the change from a conventional fuse to a time-delay fuse not only improves the protection obtained but, at the same time, effects a very desirable saving in weight, the time-delay device weighing only one-sixth that of the conventional fuse. If the blocks in which the fuses are mounted also are considered, the saving in weight is even more pronounced.

The counterparts of the commercial power fuses which have time lag and are of conventional design are found in the Air Corps specification 94-32084 and 32084-*B*. Specification 32084-*B* did not require the fuse to have any inherent time lag, its characteristic being comparable to a power fuse of conventional design. As already mentioned, specification 94-32084 required the time lag found in fuses combining a thermally operable current interrupter with a short-circuiting member. A 100-ampere 32084-*B* fuse needed for a 27-ampere motor weighs 71 grams, whereas a 30-ampere fuse made in accordance with specification 94-32084, giving complete protection to a 27-ampere motor, weighs only 22 grams. Therefore, contrary to the general conception, com-

plete protection is effected with a saving in weight instead of increasing the weight of the fuse, even though ampere for ampere the time-delay fuse weighs more.

As the ceiling of aircraft has been pushed steadily upward, the ambient-temperature range to which it is exposed has increased until now −60 to +71 degrees centigrade is not unreasonable to expect. Since the usual procedure is to mount the electrical protective devices in a central location, the equipment seldom would be exposed to the same ambient temperature as its protective device. Such a central or grouped mounting is necessary when circuit breakers combining a switch with electrical protection are used but is unnecessary when the switching operation is divorced from the protector as is possible with fuses. Then the fuse can be mounted wherever convenient, and, if the type and size fuse is chosen intelligently, the ease of replacement need not be a factor. The fuse only will operate when there is a case of trouble and the electric equipment it is protecting already has been operated to its thermal or electrical breaking point, so that replacement before clearing the trouble accomplishes nothing.

The extreme ambient requirements imposed upon all the airplane electric equipment have been appreciated, but no reasonable solution is possible so long as the electrical protective devices are subjected to ambient temperatures different from those to which the electric equipment itself is exposed. Since the normal grouping now employed in aircraft precludes the possibility of the equipment and protective-device ambient temperatures being the same, the effect is alleviated partially by making the electrical protective device irresponsive to ambient temperatures and then building enough overload capacity in the electric equipment so that it will function satisfactorily at the higher ambient temperature. Since the capacity of most electric equipment decreases with increasing temperature, it is overfused at the lower ambient temperatures and underfused at the higher ones, because the protective device is designed to operate purely on current, not ambient temperature. The net result is that accurate fusing is impossible as long as the protective device is not responsive to temperature in the same manner as the equipment it is protecting and as long as the time–current curve is not identical over the entire range of currents which flow through the equipment.

The time-delay fuse already described can be biased to give the same response to ambient conditions as the electric equipment it is protecting by choosing the operating point of its thermal interrupter properly. Since the operation of the thermal interrupter in the fuse is dependent on temperature resulting from the heat generated in the heater of the fuse as well as on the ambient temperature, the bias needed to compensate for any given range of ambient temperatures can be obtained by the choice of the operating point

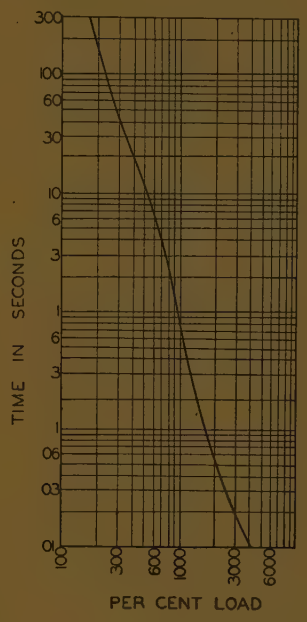


Figure 2. Time–current characteristic of the time-delay fuse shown in Figure 1

of the thermal interrupter. An even closer thermal relationship between the electric equipment and its protector is accomplished in power applications by mounting the time-delay fuse on the equipment itself in good thermal relation to the hot spot. This thermal relation is obtained by a metal pin which extends into the equipment from one terminal of the protective device. To improve the flexibility of the design, two points are provided which are responsive to thermal energy. The first, located between one terminal and the heat coil (see Figure 3), operates almost entirely from the heat supplied it by thermal conduction from the hot spot of the device being protected. The second thermal interrupter, located between the heat coil and the short-circuiting strip, is biased very slightly by the heat conducted to it from the hot spot, through the heat coil. In most cases the temperature of the heat coil is above that of the terminal, so that heat conduction from the terminal to the second interrupter through the coil is impossible, but the biasing effect of the thermal conduction from the hot spot is realized in that the terminal temperature is elevated by it, so reducing the temperature gradient from the thermal center of the coil to the terminal and therefore reducing the heat conduction, thereby forcing more of the heat generated in the coil to flow into the second thermal interrupter. The operation of the thermal interrupter between the heat coil and the short-circuiting strip is due primarily to the heat generated in the heat coil and is a function of the current flowing through it. Since the heat must be conducted from the coil to the interrupter, a time lag is obtained which can be varied by varying the mass of the thermal interrupter or by varying the length of the thermal path through the heat coil. The short-circuiting strip, of course, is designed to be fast acting, but its rating is so chosen that it does not function until the higher overloads, approaching short-circuit conditions, are realized.

Such a protective device has proved its worth in industry. The flexibility of its design permits the time–current curve to be made the same shape as that of the equipment it is protecting, so that complete protection is furnished over the entire operating range of the equipment under any ambient conditions. It will operate, protecting the equipment, whenever the temperature of the hot spot exceeds a safe predetermined temperature. Again the only answer to the objection that the protective device is unsuited for aircraft applications, because it often must be mounted in an inaccessible place which cannot be reached in flight, is that such a protective device eliminates the guesswork in fusing so that the protective device only will function when the equipment is worthless. The continued use of the equipment under such conditions only will disturb the electric system if not injure it without producing any useful work. If the equipment itself is accessible enough to be repaired in flight, then, obviously, the protective device also is replaceable, so that, in either case, the positive reliable performance of a properly

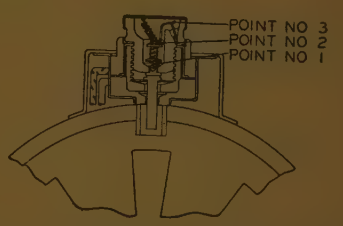


Figure 3. Time-delay fuse consisting of three interrupters in series

Point 1 is responsive to the heat conducted to it from the device being protected; point 2 is responsive to the heat conducted to it from the heat coil; and point 3 is a fast-acting short-circuiting member functioning at high overloads

designed and properly selected protective device cannot be criticized.

Such a time-delay fuse never has been adapted to aircraft applications, but it could be made quite attractive from a weight angle, because the present commercial model, in which weight has not been given any special consideration, weighs only 23 grams for a ten-ampere rating, whereas a 94-32084 fuse of the same rating weighs 18 grams. Hence, a few minor design changes probably could reduce the weight to a figure quite comparable with that of the 94-32084 fuses which have proved quite satisfactory.

Circuit Protection

The problem of circuit protection as distinct from the protection of electric equipment is being attacked from two angles, both of which have their advantages and limitations. The first may be termed circuit isolation, its principal function being to isolate heavy faults and remove the load from the system to prevent the overloading of the generators supplying it. The second may be termed circuit protection in that the protective device is chosen so that it will function as a result of damaging overloads as well as faults, to remove the excessive load from the system. In either type of circuit protection, fuse co ordination, both with the branch circuit fuses and the equipment fuses, must be considered to prevent cascading and the blowing of fuses which will de-energize more than the absolute minimum of equipment under fault conditions. Fortunately, on the 27-volt systems the co-ordination problem was quite simple, because a transmission system of the simplest sort was employed, and the number of points at which cascading could occur was few if any. Because of this, fuse co-ordination was accomplished by increasing the rating of the feeder fuses inordinately to prevent them blowing under fault conditions in any of the branch circuits. This practice led to the use of limiters instead of fuses in these overfused applications. Since the rating of the fuse was chosen so great that under low overload conditions the wire would be destroyed before the fuse functioned, nothing could be gained by using a fuse.

Limiters differ from fuses in that the fusing current of a limiter is great compared to the safe carrying current, whereas in a fuse the difference between the carrying current and the opening current is small. Since the limiter link usually is made of copper, silver, or other high-melting materials, there is a range of currents between the safe carrying current and the fusing current which, if continued long enough, will destroy the device itself as well as equipment in good thermal relation to it. The fuse, on the other hand, will carry or blow, depending upon the current, without ever reaching a damaging temperature. Hence, the current limiter can be compared to the fast-acting short-circuiting strip which is combined with the thermal interrupter in the time-lag fuses already described. The aircraft limiters are an adaptation of those devices used in power work which originally were little more than a cable lug with a reduced section. Their function in underground work was to assure that, in case of trouble, the blow would occur in the manhole where the limiters are mounted instead of in the duct run, thereby simplifying the clearing of trouble and the restoration of service. This feature of the limiter is of no great value in aircraft where the conduit runs are short and particularly in military applications in which open wiring is employed almost universally.

Since the limiter will destroy itself on slight overloads because of the high melting point of its link material, it is satisfactory only in clearing short-circuit or high fault currents. Its extremely low time lag gives a wider separation of blowing times between limiters of different ratings at the same current, so that coordination is relatively simple. Therefore, limiters permit the isolation of faults producing high fault currents, but on low currents they are worse than no protection at all, because they in themselves produce a fire hazard and destroy equipment associated with them.

The fault currents resulting in an electric system are dependent not only on the impedance of the fault but also on the capacity of the generating equipment. Of course, the higher the voltage, all other conditions being equal, the greater will be these currents. Hence, on the 27-volt systems the fault currents may be expected to be relatively small, whereas those encountered in the 208-volt systems will assume reasonable proportions. However, the impedance at 400 cycles will reduce these currents, so that it is not at all unreasonable to assume that low fault currents can be obtained in aircraft. As long as these currents are not great enough to injure the wire insulation, there is no reason for them to be cleared instantly, particularly if the isolation of the fault will, at the same time, isolate equipment which is functioning properly. Instead, it would be better to permit the fault to persist until the completion of the mission and then be cleared when the airplane had returned to its base. Many of the wire sizes in aircraft are chosen from a voltage-drop standpoint instead of a thermal one, so that low fault currents will produce no damage to the distribution system and will affect the equipment only in that the voltage at its terminals will be reduced because of the increased voltage drop in the line. If this drop is sufficient to cause the equipment to overheat, it will be protected by its own protective device, so that the circuit protective devices need not function in this eventuality. If the fault current is so great that the thermal capacity of the wire itself is insufficient to withstand the heat generated in it, the fault must be isolated if complete protection is to be realized.

To obtain complete protection of the electric distribution system, the problem again reverts to a consideration of the combined thermal capacity of the insulation and copper of the conductors. Again the protective device's time–current characteristic must be identical with that of the conductor to give complete protection under all conditions of load. Since the thermal capacity of the conductor is less than that of most electric equipment, the time lag of the circuit protective device need not be so great as that of the equipment protection. This problem was appreciated by the United States Army Air Corps and was covered by specification 94-32272. The time lag required in the most recent issue of this specification is a minimum of 40 seconds as compared to 90 seconds in the 94-32084 specification covering protective devices for electric equipment.

The effect of ambient temperature on the carrying capacity of the wires is as important as its effect on the capacity of electric equipment, so that again a protective device responsive to ambient temperatures in the same proportion as the wires and mounted in the same ambient temperature is needed. This is not so unreasonable as it appears on the surface, because the use of glass insulation on the wires permits a relatively high safe-operating temperature, so that the effect of the ambient temperature on the protective device need not be too pronounced. Also, the grouping of sectionalizing fuses in a common central cabinet which may be subjected to ambient temperatures very different from those of the wire is unreasonable, both from a safety angle in military aircraft and from a functional angle. Grouping the devices together necessitates the use of additional wire which, in turn, should require additional sectionalizing fuses for complete fault isolation.

Therefore, the circuit fuses can be mounted in approximately the same ambient temperature as the circuit wires and, though not so responsive to ambient temperature as the equipment protection, still should respond in the same manner as the circuit conductor.

By using a time-lag fuse of the design shown in Figure 1 but with the absorber mass decreased and the thermally operable current interrupter biased to be less responsive to ambient conditions than the equipment protective device, a saving in weight is accomplished, and a circuit protective device is obtained which will co-ordinate beautifully with the equipment protector. Since both the equipment and the circuit protectors have fast-acting short-circuiting members, excellent co-ordination is obtained at the higher overloads with complete protection to equipment and circuit under every conceivable condition of overload and ambient temperature. Thus, by adapting an arrangement already proved in power applications, aircraft can avoid many headaches, if not catastrophes, as its power system grows from that of an automobile to that of a power network.

References

1. Present D-C Aircraft Electric-Supply Systems, **H. C. Anderson, S. B. Crary, N. R. Schultz.** AIEE Transactions, volume 63, 1944, June section, pages 265–72.

2. Basic Considerations in Selection of Electric Systems for Large Aircraft, **W. K. Boice, L. G. Levoy, Jr.** AIEE Transactions, volume 63, 1944, June section, pages 279–87.

3. Electrical Aircraft Systems Reduce Vulnerability of Planes, **C. J. Breitwieser.** *Product Engineering*, December 1943, pages 778–81.

4. Aircraft Electricity (book), **N. J. Clark, H. E. Corbitt.** Ronald Press, 1942.

5. Parallel Operation of Airplane Alternators, **D. W. Exner.** AIEE Transactions, volume 62, 1943, December section, pages 755–60.

6. High-Output Generating Systems, **W. F. Fell.** *Aviation*, June 1943, pages 167–70, 330–2.

7. Electric-Circuit Burning-Clear and Damage Phenomena on Aircraft Structures, **C. M. Foust, J. G. Hutton.** AIEE Transactions, volume 63, 1944, April section, pages 198–204.

8. Optimum Voltage for Airplanes, **V. H. Grant, M. F. Peters.** Electrical Engineering, volume 58, October 1939, pages 428–31.

9. Applications of Electric Power in Aircraft, **T. B. Holliday.** Electrical Engineering, volume 60, May 1941, pages 218–25.

10. Why Tomorrow's Planes Will Have A-C Electrical Systems, **T. B. Holliday.** *Electrical Manufacturing*, July 1943.

11. Temperature Co-ordination of Relays and Fuses. **M. R. Howell.** *Electrical World*, July 11, 1942, pages 48–50.

12. Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts or Less, **B. W. Jones.** AIEE Transactions, volume 61, 1942, July section, pages 483–7.

13. Electric-Circuit Fault-Protective Principles as Applied to D-C Aircraft Systems, **R. H. Kaufmann.** AIEE Transactions, volume 63, 1944, pages 333–44.

Switchgear and Control for an Electronic Power Converter

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NONMEMBER AIEE

Synopsis: The electronic power converter, controlled by the equipment described in this paper, consists of two units rated 10,000 kw installed and operating at the Edgar Thomson Works of the Carnegie-Illinois Steel Corporation. The power converters connect a 60-cycle 69-kv system and a 25-cycle 44-kv system with a permissible power transfer of 20,000 kw in either direction between the systems. Transfer of power by rectification and inversion and the design of the electronic power converter are discussed in companion papers.[1,2] The switchgear and control devices are discussed in this paper and for convenience and clarity, the discussion is divided into three main headings: "Switching," "Control," and "Protection."

TO the switchgear designer whose point of view is influenced so largely by the needs of the "man who operates and maintains," the electronic power converter presents some new and interesting opportunities. The ease with which the converter is switched in o and out of service and its output is controlled presents the opportunity of providing simple and at the same time versatile controls by means of which the operator can make the fullest use of the converter, in meeting the varying demands of his system. Protective relaying of the electronic converter against system disturbances is simple since the rectifier furnishes no power to its system and the output of the inverter is limited.

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Switching

An examination of Figure 1 will reveal that the switching of the main power circuits of the electronic power converter is of the simplest possible nature. The unit is switched into service merely by closing power circuit breakers connecting the transformers to the two systems. Aside from having the auxiliary and ignition bus energized by closing the auxiliary and ignition transformer switch and circuit breaker, the only prerequisite to putting the unit into service is that the ignition-control-system networks for both frequencies must be in the inverter position. Auxiliary switches on the circuit breakers and position switches on the power-control device provide circuits for establishing this condition whenever the circuit breakers are tripped, and simple interlocking in the circuit-breaker control circuits insures it. Therefore, the sequence of closing the breakers is of no consequence, and either breaker may be closed first, regardless of the intended direction of power flow. For convenience and simplicity of protective-relay circuits, both breakers are tripped simul-

14. Aircraft Electrical Engineering (book), **R. Matson.** McGraw-Hill Book Company, Inc., New York, N. Y., 1943.

15. A 600-Volt Enclosed Limiter for Network Use, **P. O. Langguth, H. L. Rawlins, J. M. Wallace.** AIEE Transactions, volume 61, 1942, July section, pages 536–8.

16. Electrical Power in Aircraft, **G. W. Ledbetter.** *Aero Digest*, February 1942, pages 163–70.

17. Aircraft Contactors, **F. J. Russell, A. P. Charbonneau.** AIEE Transactions, volume 62, 1943, September section, pages 563–6.

18. Principles of Aircraft Electric-Motor Protection, **V. G. Vaughan.** AIEE Transactions, volume 62, 1943, December section, pages 760–5.

19. Industrial Network Limiter Developed, **J. M. Wallace.** *Electrical World*, October 3, 1942, page 88.

20. Short-Circuit Protection of Distribution Networks by the Use of Limiters, **C. P. Xenis.** AIEE Transactions, volume 56, 1937, September section, pages 1191–6.

21. Operating Record Proves Value of Limiters, **C. P. Xenis, E. Williams.** *Electrical World*, October 21, 1939, pages 51–3.

22. Limiters Prevent Cable Faults From Spreading on Networks, **C. P. Xenis, E. Williams.** *Electrical World*, October 7, 1939, pages 46–9.

23. Aircraft Electric Power-Supply System, **J. E. Yarmack.** AIEE Transactions, volume 62, 1943, October section, pages 655–8.

24. Airplane Motors—A Year of Progress, *Electrical Manufacturing*, August 1943, pages 99-103, 170–6.

25. Control Components That Solve Some Aircraft Problems. *Electrical Manufacturing*, August 1942, pages 32–5, 64, 92–100.

26. New Circuit Controls for Aircraft Reflect Wartime Specialization. *Electrical Manufacturing*, October 1943, pages 116–18, 196–202.

27. Electrical Protective Devices For Aircraft, **J. C. Lebens, Jr.** *Aero Digest*, March 1943, pages 235–9, 379–80.

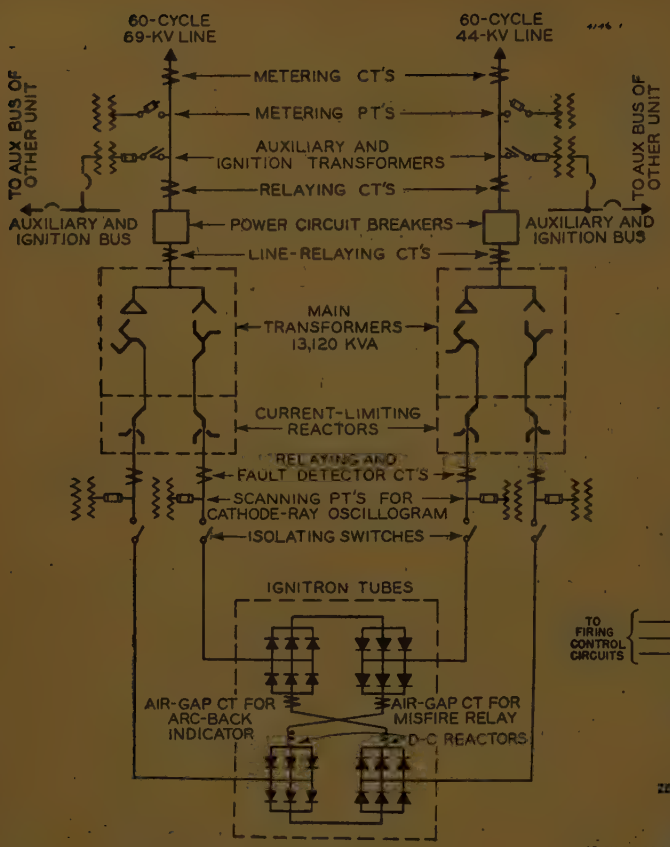


Figure 1. Simplified one-line diagram showing switching equipment

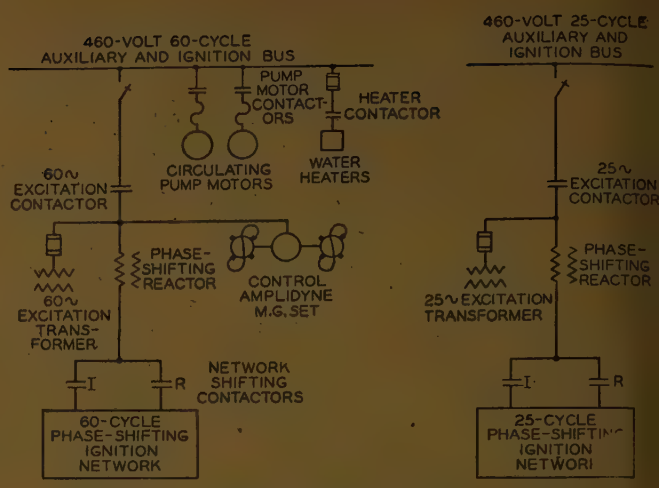


Figure 2. Switching equipment for auxiliary apparatus

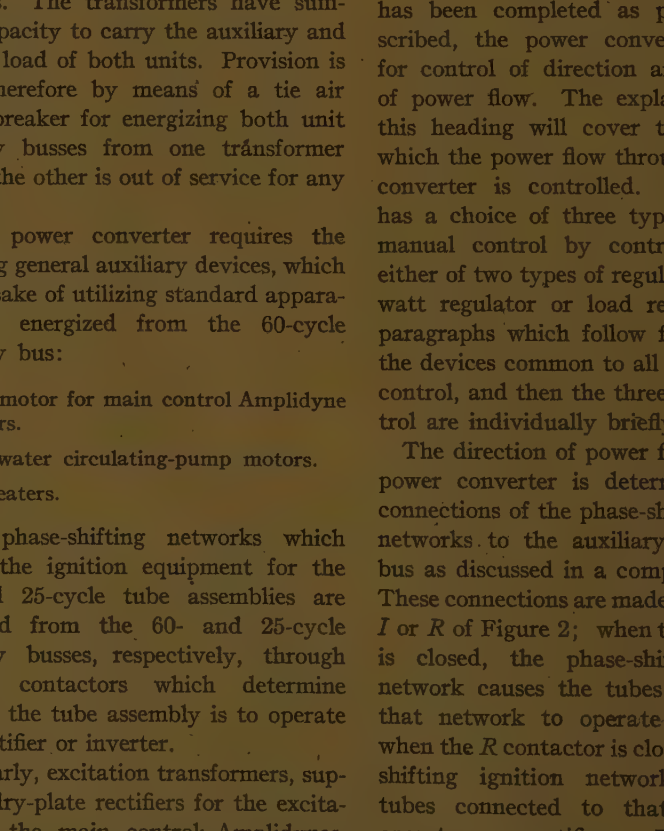


Figure 3. Contro device for powe flow through th power converter

taneously at all times. Circuit-breaker operation is the only switching operation required under normal operating conditions.

The only switching required in the circuits between the ignitron tubes and the transformers is that required for safety to personnel who may have occasion to work on equipment in the tube assembly enclosure. For this purpose, enclosed group-operated disconnecting switches are provided, suitably key-interlocked with tube-enclosure doors and high-voltage power circuit breakers to insure safety and to prevent operation of the switches under load.

Since the flow of power in the d-c portion of the circuit is under complete control of the grids of the rectifier and inverter tubes, there is no necessity for any switching apparatus in the d-c circuit.

Switching of the auxiliary and ignition transformers to the high-voltage lines is accomplished by horn-gap disconnecting switches suitable for interrupting magnetizing current of the transformers. These switches are suitably key-interlocked with the transformer secondary air circuit breakers to prevent their operation under load. The transformers are protected by suitable current-limiting fuses.

The 460-volt secondary windings of the auxiliary and ignition transformers are connected to their respective loads by means of manually operated air circuit breakers. The transformers have sufficient capacity to carry the auxiliary and ignition load of both units. Provision is made therefore by means of a tie air circuit breaker for energizing both unit auxiliary busses from one transformer in case the other is out of service for any reason.

Each power converter requires the following general auxiliary devices, which for the sake of utilizing standard apparatus are energized from the 60-cycle auxiliary bus:

Driving motor for main control Amplidyne generators.

Cooling-water circulating-pump motors.

Water heaters.

The phase-shifting networks which supply the ignition equipment for the 60- and 25-cycle tube assemblies are energized from the 60- and 25-cycle auxiliary busses, respectively, through selector contactors which determine whether the tube assembly is to operate as a rectifier or inverter.

Similarly, excitation transformers, supplying dry-plate rectifiers for the excitation of the main control Amplidynes, are energized from the 60- and 25-cycle auxiliary busses.

The switching devices in these various auxiliary circuits are shown in Figure 2.

Control

When switching of the power circuit has been completed as previously described, the power converter is ready for control of direction and magnitude of power flow. The explanation under this heading will cover the means by which the power flow through the power converter is controlled. The operator has a choice of three types of control: manual control by control switch, or either of two types of regulator control—watt regulator or load regulator. The paragraphs which follow first deal with the devices common to all three types of control, and then the three types of control are individually briefly explained.

The direction of power flow through the power converter is determined by the connections of the phase-shifting ignition networks to the auxiliary and ignition bus as discussed in a companion paper. These connections are made by contactors *I* or *R* of Figure 2; when the *I* contactor is closed, the phase-shifting ignition network causes the tubes connected to that network to operate as inverters; when the *R* contactor is closed, the phase-shifting ignition network causes the tubes connected to that network to operate as rectifiers. Therefore, for

xample, to have power flow through the onverter from the 60-cycle system to the 5-cycle system, the *R* contactor of the 0-cycle network is closed and the *I* ontactor of the 25-cycle network is losed. The *I* and *R* contactors therefore re the direct means by which direction f power flow is established. The control f those contactors is described in the ollowing paragraphs.

The magnitude of power flow through power converter is determined by the nagnitude of the reference voltage used n the field circuits of the control Ampli-lyne associated with the firing circuits, s discussed in a companion paper.[2] The reference voltage is obtained from n induction voltage regulator, and the nagnitude of that voltage is determined y the induction regulator's rotor posi-ion. Control of the rotation of the nduction-regulator rotor therefore is the lirect means by which the power-con-erter load is controlled.

The power-control device is shown in Figure 3. The rotor of the induction egulator, the Selsyn transmitter, and he cam-operated position switches are n a common shaft; the shaft is driven hrough a high-ratio gear box to give mooth vernier control of shaft position. Contacts of the cam-operated position witch control the *I* and *R* contactors (of Figure 2). For the position of the nduction regulator corresponding to 90 legrees retard excitation and (zero power low) for rectifiers, as discussed in a ompanion paper,[2] the contacts of the am-operated position switch cause the contactors of both the 25-cycle and 60-ycle networks to be closed, and all tubes f the power converter operate as invert-rs. As the induction-regulator rotor s moved away from this position of ero power flow, by means of contacts f the cam-operated position switches, he *I* contactor of one of the networks is pened and the *R* contactor is closed, naking the power converter capable of ower transfer; for one direction of otation of the induction-regulator rotor rom its zero power flow position the *R* ontactor of the 25-cycle network is losed, and for the other direction of otation the *R* contactor of the 60-cycle etwork is closed. Also, as the induction-egulator rotor is rotated in either direc-ion from the zero-power-flow position, ts output voltage magnitude is varied o give smooth increase from zero to naximum power flow as discussed in a ompanion paper.[2] The power flow hrough the power converter for different ositions of the induction-regulator rotor s illustrated in Figure 3 (below the in-duction voltage regulator); this illustration represents 360 mechanical degrees of rotation of the rotor. It is at points *A* and *C* that the *R* contactor of the 60-cycle or 25-cycle network, respectively, closes and remains closed over the travel represented by the cross-hatched areas in the illustration. Rotor rotation from *A* to *B* or from *C* to *D* represents gradual smooth increase in power flow from zero to maximum for either direction of power flow as marked. Two contacts of the cam-operated position switch act as limit switches and prevent travel of the induction-regulator rotor beyond points *B* and *D*. Indication of the position of the induction regulator is obtained on the control board from the Selsyn receiver shown in Figure 3.

Driving power for the shaft driving the induction regulator, Selsyn, and cam-operated position switches is obtained from a d-c motor whose armature is energized by an Amplidyne generator. The use of a generator rather than a constant potential d-c source to energize the d-c motor has the advantages of:

1. Reversible rotation of the d-c motor without the use of reversing contactors.
2. Easy adjustment of d-c-motor armature voltage magnitudes.
3. Easy adoption of control circuits.
4. Inherent dynamic braking.

The Amplidyne is used instead of a conventional d-c generator, because its low excitation requirements permit the use of relay and control-switch contacts directly in its field circuit; the control fields of the Amplidynes used require only 0.1 ampere for 300-volt output; also these control fields have a resistance of only 218 ohms which permits the use of low control voltages.

The Amplidyne has three fields, two control fields designated as *F1*-*F2* and *F3*-*F4* and one field designated as *F7*-*F8* used for antihunt and residual voltage killing. To produce rotation of the d-c motor, voltage is applied to one of the Amplidyne control fields. Either direction of rotation is obtained as desired, since the control circuits apply a voltage of either polarity to a given field terminal. Separate "raise" and "lower" speed adjustments are provided, since operating engineers may find it desirable to decrease the load setting at a faster rate than the load setting is increased, or vice versa. A fault-blocking contact in the control-field circuits opens during arc-backs or other faults within the power converter; this prevents change in the setting of the induction voltage regulator during such faults while the fault-recovery equipment is functioning.

As stated in the section on switching, the main circuit breakers can be closed only if excitation power is available and if both the 25-cycle and 60-cycle portions of the converter are operating as inverters. To insure that excitation is present, an undervoltage relay is connected to the auxiliary and ignition bus (Figure 1), and its contact permits the breakers to close only when proper voltage is present. Contacts of the cam-operated position switch (Figure 3) prevent closing of the breakers except when the induction voltage regulator is in its zero-power-flow position where all tubes operate as inverters. If the circuit breakers are tripped while the converter is carrying load, the induction regulator automatically is returned to its zero-power-flow position.

Manual control of power flow is by means of a simple double-throw momentary-contact control switch. Amplidyne field *F3*-*F4* is used for manual control, one position of the control switch connecting terminal *F3* to positive potential, and the other position of the switch connecting terminal *F3* to negative potential (control potential is 24 volts direct current). An operator, therefore, merely turns this control switch in the position to give the desired direction of power flow, and the switch is held in that position until power flow has increased to the desired magnitude as indicated by a wattmeter on the control board.

The watt regulator is a device for regulating automatically the power flow through the power converter. The power elements of the device are connected to the current transformers and potential transformers in the power circuit of the power converter (Figure 1); the potential elements are correctly connected to the potential transformers for either direction of power flow by means of contacts of the position switches. The watt regulator has two sets of contacts; one set of contacts is closed when the power-converter power flow is greater than the regulator calibration, and the other set of contacts is closed when the converter power flow is less than the regulator calibration; both sets of contacts are open when the converter power flow is equal to or within approximately ±2 per cent of the regulator calibration. Calibration of the device is changed by means of a control switch on the control board; an instrument is provided on the control board which indicates the calibration setting directly in kilowatts.



Figure 4. Control-room panels and excitation cubicles for 10,000-kw electronic power converters



Figure 5. Load-control panels for each converter unit and test panel at right

The two sets of contacts connect the Amplidyne control field to control voltage of correct polarity to cause the induction voltage regulator rotor to be rotated in the direction to correct the power flow; for example, if the power flowing through the converter is less than the setting of the watt regulator, its contacts connect the Amplidyne control field to the potential which will cause the d-c motor to rotate the induction regulator rotor in the direction to increase power flow through the power converter. Amplidyne control field *F*3-*F*4 is used for watt regulator control as well as for manual control. With the watt-regulator control switch in the "on" position, the regulator contacts are connected to field *F*3-*F*4 and the manual-control-switch contact circuits are disconnected. Contacts of the cam-operated position switch connect the regulator contacts to the control voltage polarity which gives correct direction of rotation of the induction voltage regulator for either direction of power flow in the frequency changer. In putting a power converter on watt regulator control, it is initially necessary for the operator to select direction of power flow by means of the manual-control switch; once the induction-voltage regulator has arrived at either position *A* or position *C* (Figure 3), the regulator control switch can be turned to the "on" position, and the regulator functions to pick up the load for which the regulator is set.

The load regulator is an indicating integrating device energized from current and potential transformers which measure the interchange of power between the power company's system and the industrial company's 60-cycle system. The load regulator is designed to hold a preselected value of instantaneous exchange kilowatts. This value, however, is corrected to compensate for any differential between the ideal preselected kilowatt-hour exchange and the actual kilowatt-hour exchange as time progresses.

The load regulator permits the industrial user to make the best possible exchange of kilowatt-hours with the power company as determined by his contract with the power company. When the 60-cycle industrial load is heavier than the setting of the load regulator, the power converter supplies kilowatt-hours from the 25-cycle system to the 60-cycle system; when the 60-cycle industrial load is smaller than the setting of the load regulator, the power converter supplies kilowatt-hours from the 60-cycle system to the 25-cycle system. The contacts of the load regulator connect Amplidyne field *F*1-*F*2 to the control voltage polarity which correctly adjusts the power flow through the power converter. The load regulator is made operative or is removed from operation by a two-position control switch.

For the station described, which has two converters, the load regulator can be used to control either or both converters by selecting such operation by means of the control switches. When both power converters are operated on load-regulator control, load-balance relays are made operative. The load-balance relays are two-element induction-disk devices whose current circuits are energized from differentially connected current-transformer circuits of the two power converters. These relays keep the loads on the two power converters approximately equal when both are controlled by the load regulator.

Any of the types of control described, manual, watt-regulator or load-regulator, can be used on one converter without regard for the type of control being used on the other. While the operator has a versatile choice of control combinations at his disposal, there are no complicated operating sequences which must be followed.

Protection

For the purpose of discussion of protective relaying, faults which must be considered may be classified and subclassified as follows:

1. Faults associated with the converter its transformers, and auxiliary apparatus
(*a*). Insulation failures in the transformer and a-c conductor system.
(*b*). Malfunction of the ignitron tubes or insulation failure in the d-c conductor system accompanied by failure or too frequent operation of the automatic fault-suppression features of the converter grid and ignition control.

2. Faults associated with the power systems
(*a*). Short circuits.
(*b*). Loss of voltage.

Insulation failures in the transformers and a-c conductor system are treated in the same manner as similar faults in conventional transformers. It is impractical to apply differential relays to rectifier transformers by reason of the complexity of their secondary windings and the peculiar character of the currents flowing in these windings. Therefore, the best available protective relay for this purpose is the short-time-delay induction overcurrent relay with an inverse characteristic. This relay is adjusted to have just sufficient minimum time delay (approximately 0.05 second) to allow for normal transformer magnetizing inrush currents and short-duration fault currents resulting from transient disturbances in the ignitron tubes. On systems having impedance-grounded neutrals a sensitive residual overcurrent relay also is used to clear ground faults.

Normal suppression of arc-backs and "shoot-throughs" in the rectifier and inverter tubes and d-c short circuits by

Differential Generator Control Relay for D-C Aircraft Electric Systems

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ASSOCIATE AIEE

Synopsis: The definite-pickup type of generator control relay has been in use for a period of years on d-c aircraft electrical systems. While this type of generator control relay gives satisfactory operation when the system is in proper adjustment, it had disadvantages which are accentuated by improper adjustments of regulators and the relays. A differential type of generator control relay not subject to chatter has been developed and tested in flight on a system having four main-engine-driven generators and an auxiliary power plant. This type of relay has been shown practical and advantageous when used on a system having voltage regulators with equalizers.

Functions of Generator Control Relay

THE principal function of a generator control relay on aircraft d-c electrical systems is to connect the generator to the ship's d-c bus whenever the generator is in a condition to furnish power to the bus, and to disconnect the generator from the system whenever the generator draws reverse current from the bus above a predetermined value. The relay also provides a means of disconnecting or holding off any generator at the will of the operator.

The Definite-Voltage-Pickup Type of Relay

In the past on 28.5-volt d-c electrical systems, a relay of the voltage-pickup type has been in common use. Such a scheme is illustrated schematically in Figure 1.

The voltage-pickup type of generator control relay consists of a main contactor energized from the generator ter-

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The tests referred to in this paper and oscillograms were taken in flight by R. H. Kauffman and C. W. Hanson of the General Electric Company.

he converter ignition and grid-control equipment and the igniter short-circuiting device has been discussed in a companion paper[1] describing that equipment. Operating experience so far has developed no failure of the fault-suppression equipment to clear arc-backs, "shoot-throughs" and d-c short circuits. However, such a possibility, though considered very remote, must be guarded against by backup protective relays which will prevent damage to the apparatus under such circumstances.

Rectifier arc-backs which are not cleared by the normal fault-suppression apparatus will result in sustained overcurrent in the rectifier transformer secondary winding of sufficient magnitude to cause the transformer protective overcurrent relays in this circuit to operate.

Inverter "shoot-throughs" and d-c short circuits which are not cleared by the normal fault-suppression apparatus will result in sustained overcurrents in the rectifier transformer and will produce alternating overcurrents in the lines between the rectifier tubes and the transformer; sensitive and effective protection is obtained by the use of short-time induction overcurrent relays connected to current transformers in these lines.

In the case of repeated arc-backs or 'shoot-throughs," the fault-suppression apparatus merely will suppress these faults as fast as they occur, and some means must be provided for disconnecting the converter when such faulting occurs. Since each fault suppression is accompanied by a transient operation of an igniter short-circuiting device, impulses from the operation of this device are used to actuate a relay which operates to trip the power circuit breakers if the impulses occur too rapidly for predetermined time. The relay used for this purpose is a combined time-delay and notching device which, when subjected to more than a predetermined number of impulses within a predetermined period of time, closes its contacts to trip the power circuit breakers.

Short circuits on the supply system tend to reduce the voltage of that system. This reduced voltage applied to the rectifier tends to decrease the normal flow of power from the supply system to the receiving system. Since the inverter cannot function as a rectifier or the rectifier as an inverter unless the grid controls purposely are changed to give such operation, the converter cannot transfer power from the receiving system into the supply system.

Short circuits on the receiving system of such a nature that they do not reduce the voltage of that system below approximately 80 per cent of normal will cause the converter to deliver only a nominal increase in power to the receiving system, but never more than the upper limit of power transfer provided for in the converter control.

Short circuits on the receiving system of sufficiently low impedance and on any or all phases will cause the inverter to cease to commutate the direct current. This in effect places a short circuit on the d-c system. The rectifier control immediately limits this direct current to normal maximum load current, but the inverter immediately ceases to deliver power to the receiving system. As soon as the fault on the receiving system is cleared and the voltage returns to normal, the inverter begins to commutate again and delivers normal power to the receiving system.

As previously noted, low voltage, either single- or three-phase, on the supply system merely decreases the normal flow of power or, if it is too low, causes the rectifier to cease firing, thereby interrupting the flow of power. Low voltage, either single- or three-phase, on the receiving system either causes a nominal increase in power flow or causes the inverter to cease commutation, thereby interrupting power flow.

It becomes evident, therefore, that only in case of prolonged low voltage on the receiving system, sufficient to cause a cessation of inverter commutation, is it necessary to disconnect the converter from the system. This is accomplished readily by means of a single- or three-phase time-delay undervoltage relay set to drop out at approximately 80 per cent of normal voltage and having sufficient time delay to permit system relaying to remove receiving-system short circuits.

References

1. THE ELECTRONIC CONVERTER FOR EXCHANGE OF POWER, F. W. Cramer, L. W. Morton, A. G. Darling. AIEE TRANSACTIONS, volume 63, 1944.

2. DESIGN OF AN ELECTRONIC FREQUENCY CHANGER, C. H. Willis, R. W. Kuenning, E. F. Christensen, B. D. Bedford. AIEE TRANSACTIONS, volume 63, 1944.

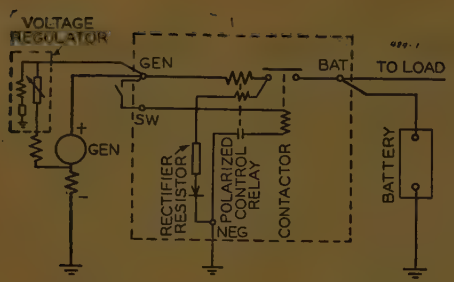


Figure 1. Schematic diagram of voltage-pickup type of generator control relay

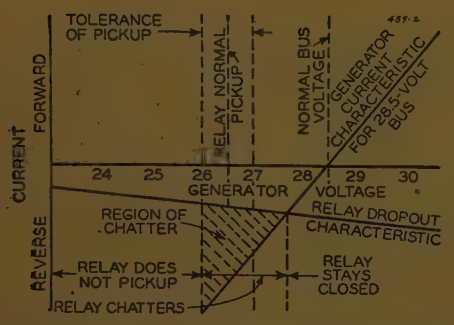


Figure 2. Characteristics of definite-voltage-pickup type of generator control relay and system

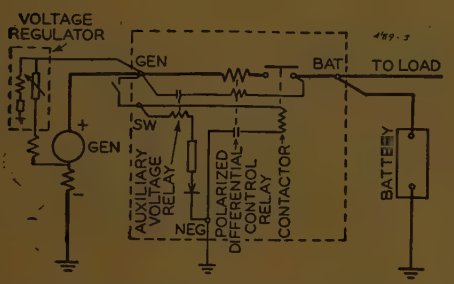


Figure 3. Schematic connections of differential generator control relay

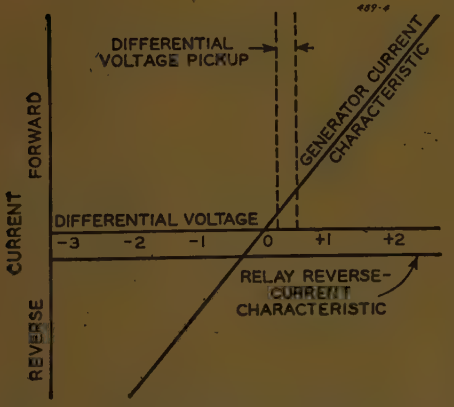


Figure 4. Characteristics of differential generator control relay and system

minal voltage through contacts on a polarized control relay having a voltage coil and a current coil. The characteristics of relays of this type are a definite voltage pickup and a dropout on reverse current which is a function of the voltage on the voltage coil, that is, it requires a higher value of reverse current to drop the relay out at 28.5 volts than

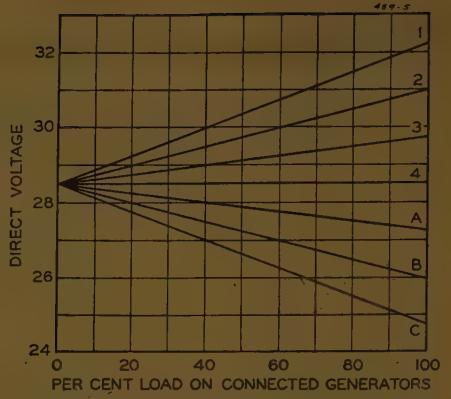


Figure 5. Characteristics of generator voltage regulators with equalizers on four-generator system

Curves 1, 2, 3—Characteristics of regulators on machines off the bus

Curves *A*, *B*, *C*—Characteristics of regulators on connected machines

Curve 4—Characteristics of regulators with all machines on

Curves 1 and *A*—One machine off, three machines on

Curves 2 and *B*—Two machines off, two machines on

Curves 3 and *C*—Three machines off, one machine on

at a lower voltage such as 27.0 volts. Typical characteristics of such a relay are shown in Figure 2, together with the system current characteristic for an assumed bus voltage of 28.5 volts established by other generators already on the bus. By reference to this figure, it can be seen that there is a shaded region above the pickup voltage of the relay where the relay will pick up and immediately upon closure draw a reverse current sufficient to drop it out and result in a chattering condition so long as the generator operates at a voltage in this region.

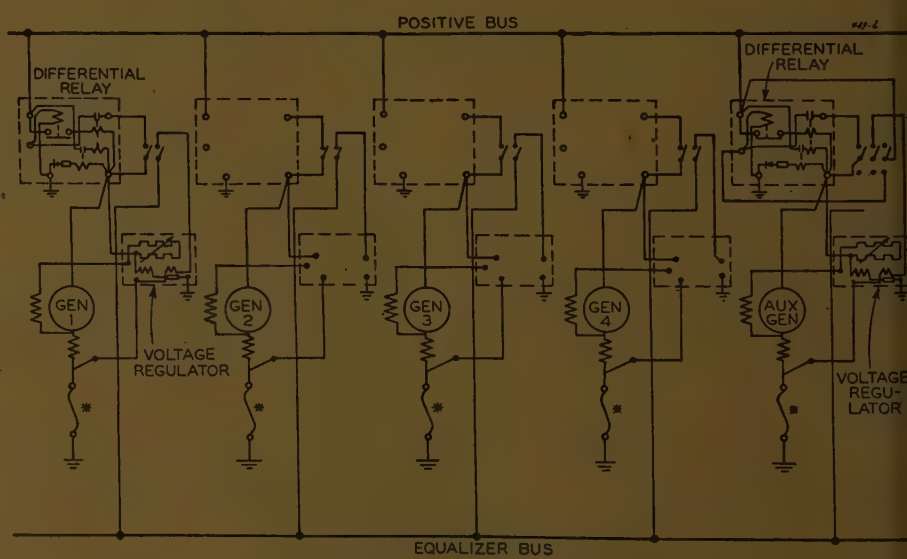
Each time a generator comes up in speed, it operates in this region and the relay will chatter unless the generator speed is brought up so rapidly that the time duration of operation in this region is so short that the chatter does not occur. A second reason why a generator may operate in this region is in case the system load is light and its voltage regulator is set low in voltage with respect to the setting of voltage regulators on other generators operating in parallel. This is the most dangerous type of chatter because it is more likely to persist for long periods of time. It has been a real source of trouble on present systems in military service as it may result in hundreds of thousands of operations of the relay in a few hours giving rise to rapid wear. The chatter may be minimized or eliminated by raising the pickup voltage, increasing the reverse current characteristic of the relay, and by making sure that voltage regulator settings and connections are proper. If these things are done the system is practical.

The Differential Type of Generator Control Relay

A second type of relay operating on a different principle has been developed which has several advantages which will become apparent as its characteristics are examined. This is known as a differential type of generator control relay and is the chief subject of this paper. Typical connections of such a relay are shown in Figure 3. Referring to Figure 3, the differential generator control relay consists of a main contactor energized

Figure 6. Schematic connections of 28.5-volt d-c system with four main generators and one auxiliary power plant

* 0.5-volt drop in negative lead for 100 per cent rated current of generator



hrough a contact on the differential control relay and a manual switch from the generator voltage. The differential control relay is a polarized device having a series coil and a voltage coil. The voltage coil is connected across the contacts of the main contactor through a contact on a small polarized voltage relay. The function of this latter voltage relay is to protect the differential voltage coil for large differential voltages and to prevent drain on the batteries in case the battery switches are left closed when the plane is not in operation. The characteristics of the differential relay are that it will close whenever the voltage of the generator is above the bus voltage by from 0.2 to 0.5 volt and it will drop out at a definite value of reverse current independent of the voltage of the generator. These characteristics are shown in Figure 4, together with the system current characteristic. It can be seen from this figure that there is no region of chatter because the relay will not close the generator on the bus under any condition where it will cause flow of reverse current. Likewise the relay will not automatically remove the generator from the bus on reverse current under a voltage condition that would cause it to reclose.

It might be argued that such operation could be obtained with a voltage pickup type of relay if the pickup voltage were raised to a point equal to or above the bus voltage. This is true but is not easy of attainment because of the necessary tolerance of pickup. On the differential type of relay a given percentage tolerance in the differential voltage results in only a small difference in generator voltage whereas the same percentage tolerance on the voltage-pickup type would result in a much wider variation in the actual pickup voltage. The differential type has another advantage in that the pickup voltage is related to the bus voltage so that if the bus voltage is low, the generators can come on at a lower speed and thus charge low batteries at a generator speed that could not generate enough voltage of cause a definite-voltage pickup type of relay to operate.

Effect of Equalizers on Voltage Regulators in Supplementing Differential Generator Control Relays

It is to be noted that the differential type of generator control relay will close the generator on only when the generator voltage is of correct polarity and above the bus voltage. This brings up a question on a system where generators are operating in parallel with all voltage regulators set at the same voltage as to whether the first generator to come on the bus will not raise the bus voltage to a point where the other generator control relays cannot close their generators on. Were it not for the presence of equalizing windings on the voltage regulators, this might be a valid objection. However, with equalizer windings on the regulators, any load on the connected generators will not only cause the voltage of the connected generators to droop with an increase in load but will also cause the voltage of the generators not on the bus

E_B = Bus voltage
E_{R1} = Contactor-coil voltage, relay 1
E_{R2} = Contactor-coil voltage, relay 2
I_1 = Current through relay 1
I_2 = Current through relay 2

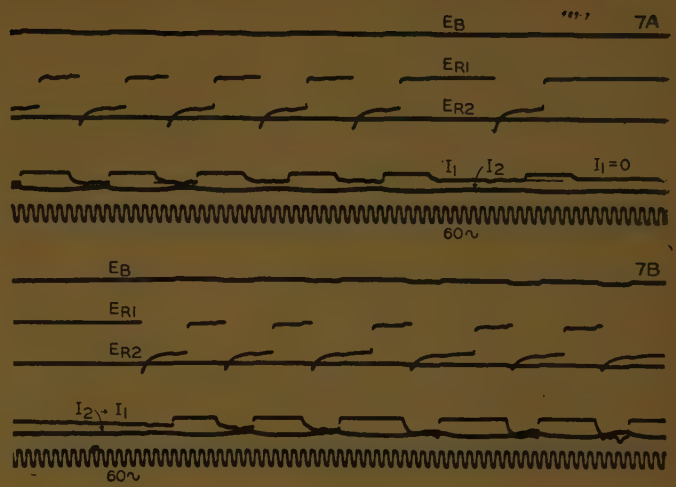
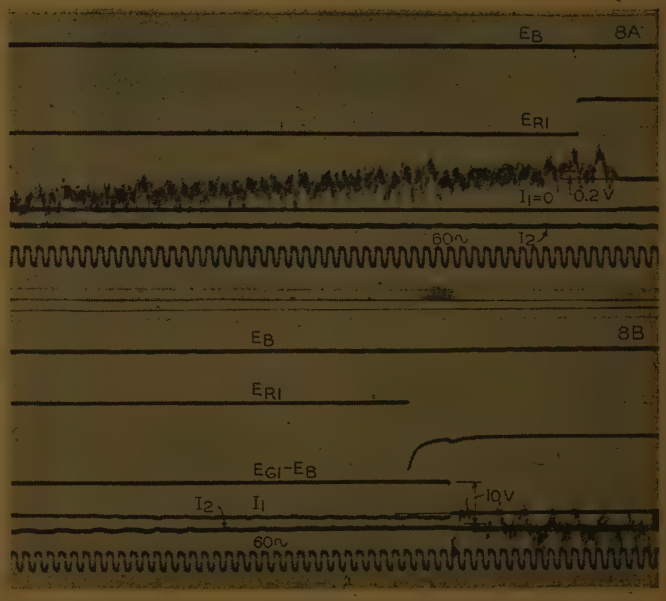


Figure 7A (upper). Oscillogram showing action at pickup on voltage-pickup type of generator control relay, taken by slowly increasing voltage setting of incoming generator regulator

Figure 7B (lower). Oscillogram showing action at dropout on voltage-pickup type of generator control relay, taken by slowly decreasing voltage of generator regulator

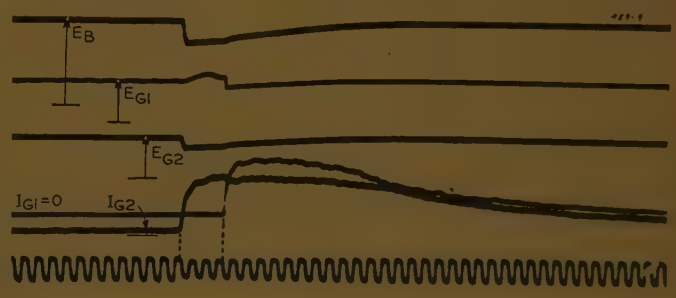
Figure 8A (upper). Oscillogram showing action at pickup on differential type of generator control relay, taken by slowly increasing voltage setting of incoming generator regulator

Figure 8B (lower). Oscillogram showing action at dropout on differential type of generator control relay, taken by slowly decreasing voltage of generator regulator



E_B = Bus voltage
E_{R1} = Contactor-coil voltage, relay 1
$E_{G1}-E_B$ = Differential voltage
I_1 = Current through relay 1
I_2 = Current through relay 2

Figure 9. Oscillogram showing pickup of one differential relay upon application of heavy (turret) load. Relay previously held off by low setting of voltage regulator



E_B = Bus voltage
E_{G1} = Voltage, generator 1
E_{G2} = Voltage, generator 2
I_{G1} = Current, generator 1
I_{G2} = Current, generator 2

to rise as the load increases. This action is illustrated in Figure 5 which is drawn based upon normal equalizer strength as specified for military aircraft. It can be seen that a differential voltage of one-fourth volt is obtained on the generators yet to be connected when only five per cent load is on each of the connected machines. A differential voltage of one-half volt would require ten per cent load on each connected machine. In the appendix it is shown that the differential voltage is dependent only upon the per cent load on the connected machines, regardless of the number of generators associated with the system. These figures are based upon all voltage regulators being set alike. However, it can be said that with the differential relay set to close on at from 0.2 to 0.5 volt on a system of generators having equalizers on the voltage regulators of normal strength, if any generator fails to come on, it would not be able to furnish power to the bus under that load condition. Therefore, it might as well remain off until its voltage regulator setting is brought into line with the settings of other regulators on the system.

Another characteristic of the differential type of generator control relay has caused misunderstanding and that is the fact that it will not close a generator on to an unloaded bus because there is then no circuit for the differential coil. The existence of a very minute load or of batteries on the bus eliminates this. It is not a disadvantage because if there is no load to be supplied there is no use putting voltage on the bus. In testing these relays on a bench test, this has sometimes caused confusion where testing the relay against an open bus without batteries or load. Only a fraction of an ampere load is required to complete this circuit.

Flight Tests With Differential Generator Control Relays

In order to prove out these principles and to satisfy any who might be skeptical of the ability to close on, tests were made on a four-engined B-24 Liberator bombing plane equipped with differential relays and on the same plane equipped with voltage pickup type relays. The tests showed that either type of relay with proper adjustments was practical but that the differential type is superior. Also, the tests showed that with misadjustment of the voltage regulators, the voltage pickup type would chatter whereas the differential type did not. No trouble was encountered in bringing any generator on with differential-type relays. In fact, those who made the tests stated that with normal regulator settings, there would have been difficulty keeping them off the bus.

Tests were made and oscillograms taken for various settings of the regulators and various load conditions. Figure 6 shows the system elementary connections.

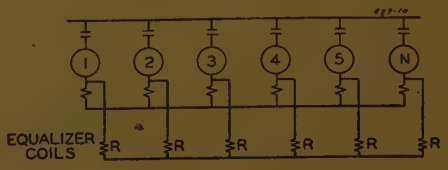


Figure 10. Schematic connection of equalizers on an *N*-machine system

These tests were purposely made severe by setting one regulator low and gradually raising its voltage setting. Figures 7 and 8 are oscillograms of these tests for the voltage-pickup and differential types, respectively. Note that Figure 7 shows a chattering condition whereas the same test with the differential relay, Figure 8, shows no chatter. Figure 9 shows a heavy load suddenly applied using differential relays with regulators set to prevent number 1 generator coming on until the application of the sudden load. It is to be noted that there was a sudden drop in bus voltage and a sudden rise in generator number 1 voltage and that generator number 1 came on within 0.05 second and carried its share of the load.

Conclusions

The differential-type generator control relay is superior to the voltage pickup type because it can be readily made to have a characteristic not subject to chatter even though the voltage regulators on generators operating in parallel do not have the same adjustment.

The voltage-pickup type of generator control relay can be made to function satisfactorily but is subject to chatter when regulators are not in adjustment on generators operating in parallel.

On a system including several generators operating in parallel, the differential type of generator control relay should give no trouble due to failure to close on the bus provided the voltage regulators are provided with equalizers of normal strength.

Less trouble with adjustment of pickup should be realized in service with the differential type than with the voltage-pickup type because a fairly wide tolerance in pickup differential voltage has a relatively minor effect on operation and will not result in chatter.

Appendix

It can be shown that for a given equalizer strength the differential voltage between machines on the bus and off the bus with all equalizer circuits closed is dependent only upon the percentage load on each connected machine. This is on the assumption that all equalizers and their leads have the same resistance and that all regulators hold constant voltage and are set for the same value except as influenced by the equalizers. Consider an *N*-machine system; see Figure 10.

The voltage applied to the equalizer circuit is determined by the load on the connected machines and the standard value is 0.5 volt for 100 per cent load. The effect of a given voltage applied to each equalizer coil is standardized. The United States Army value is a change of 2.5 volts for 0.25 volt applied to the equalizer coil of one regulator.

Let

R = resistance of equalizer coil and connected leads
N_i = number of machines on the bus
N_o = number of machines off the bus
L_i = per cent load current on each connected machine

Current in each equalizer of regulator with machine on the bus will be

$$\frac{L_i}{100}\times 0.5\times\frac{1}{N_i\left(\frac{R}{N_i}+\frac{R}{N_o}\right)}$$

Recalibration of each regulator with machine on the bus due to equalizer current is obtained by proportion as follows.

$$\frac{\frac{0.25}{R}}{\frac{L_i}{100}\times 0.5\times\frac{1}{N_i\left(\frac{R}{N_i}+\frac{R}{N_o}\right)}}=\frac{2.5}{\text{Volts recalibration}}$$

Volts recalibration below normal

$$=5\frac{L_i}{100}\frac{\frac{1}{N_i}}{\frac{1}{N_i}+\frac{1}{N_o}}$$

Current in each equalizer of regulators off the bus will be

$$\frac{L_i}{100}\times 0.5\times\frac{1}{N_o\left(\frac{R}{N_i}+\frac{R}{N_o}\right)}$$

Recalibration of each regulator with machine off the bus is obtained by proportion

$$\frac{\frac{0.25}{R}}{\frac{L_i}{100}\times 0.5\times\frac{1}{N_o\left(\frac{R}{N_i}+\frac{R}{N_o}\right)}}=\frac{2.5}{\text{Volts recalibration}}$$

Lighting Required for Commercial-Airline Aircraft

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LIGHTING plays as essential a role in the performance of a modern air-transport airplane as it traditionally does on the stage. It is the purpose of this paper to outline the extent of the subject and to acquaint the reader with the basic requirements of each of the specific applications. Actual airline operating experience will form the basis for these requirements.

The variety of problems involved in the lighting of a commercial airliner can be readily appreciated by referring to Figure 1. The percentage of the aircraft electric power supply which is required for the lighting of two representative sizes of aircraft is shown on Figure 2.

Exterior Lighting

Position Lights

In the application of lighting for navigation purposes, it is necessary that all aircraft conform to a standard arrangement so that there will be no confusion, with its unfortunate consequences. The Civil Aeronautics Administration has specified in their regulation CAR 15.20, *Position Lights*, the particular arrangement which an air carrier must employ. This system of position lighting is shown in Figures 3, 4, and 5.

With the advent of the war and increased aerial traffic, commercial-airline position lighting has undergone critical review. This survey in which the CAA, the Bureau of Standards, and the airlines collaborated consisted of installing additional lights on an airplane and flight testing. The flight tests were so conducted as to cover all the possibilities which might be encountered in air traffic. The most exacting condition of the flight test proved to be the one in which one airplane descended upon another ship flying against a background of multicolored city lights. The results of these tests indicated that a decided improvement could be gained by flashing the position lights in a uniform sequence. The immediate outcome of these tests was an amendment to the CAA regulations requiring all commercial aircraft to flash a red and white tail light alternately at a prescribed rate of 40 cycles a minute, each cycle to have the following characteristic: 150 degrees white, 10 degrees dark, 150 degrees red, 50 degrees dark. Both the red and white lights are to be fitted with 32-candlepower lamps. These red and white lights must conform in all other respects to the previous Civil Air Regulation. Permission was granted the airlines to flash all their position lights for a trial period. Figures 6 and 7 show the type of dual tail light and wing tip installations in use on a commercial airliner.

The airlines do not regard the present system of flashing lights to be the ultimate in position lighting and are acutely aware of the need for additional study of the problem. The following remarks are intended as a guide to this further study.

The existing CAR cutoff requirements effectively limit the maximum intensity of forward position lighting. If the intensity of the position light or group of lights as viewed from another airplane is considered to be a function of their relative approach velocity, then it is obvious that the maximum light intensity should be obtained when viewing the airplane from a head-on position, and this intensity should decrease in a reasonably uniform manner to an acceptable minimum when viewing the airplane from the rear. The problem of increasing the forward position-light intensity is complicated by the undesirable effect which any bright light located within the pilot's field of vision has upon his ability to discern external objects when flying through haze, light snow, or similar weather. In fact, the pilot need not be looking out of the airplane to be bothered by the flashing of a light reflecting from the haze.

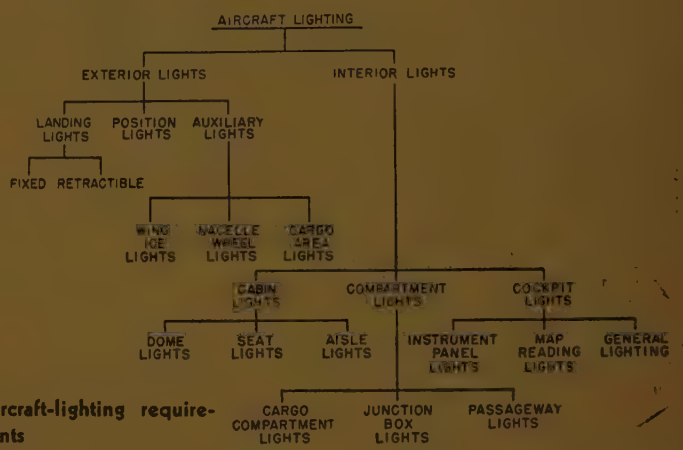


Figure 1. Airline aircraft-lighting requirements

Volts recalibration above normal

$$= 5\,\frac{L_t}{100} \times \frac{\frac{1}{N_o}}{\frac{1}{N_t}+\frac{1}{N_o}}$$

The sum of these two recalibrations is the differential voltage which is

$$\text{Differential voltage} = \frac{5 \times L_t}{100} = 0.05 L_t$$

Thus for differential voltage of 0.2

$$L_t = \frac{0.2}{0.05} = 4 \text{ per cent load}$$

For differential voltage of 0.5

$$L_t = \frac{0.5}{0.05} = 10 \text{ per cent load}$$

References

1. Application of Electric Power in Aircraft, T. B. Holliday. Electrical Engineering, volume 60, May 1941, pages 218–25.

2. The Application of Voltage Regulators to Aircraft Generators, L. W. Thompson and F. E. Crever. AIEE Transactions, volume 61, 1942, pages 363–5.

3. Present D-C Aircraft Electrical Supply Systems, H. C. Anderson, S. B. Crary, and N. R. Schultz. AIEE Transactions, volume 63, 1944, June section, pages 265–72.

Paper 44-91, recommended by the AIEE committee on air transportation for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 12, 1944; made available for printing May 4, 1944.

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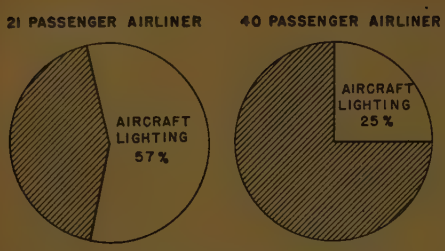


Figure 2. Relative power requirements of aircraft lighting equipment

these three phases of the landing, the airplane usually has three different attitudes with respect to the ground. With the fixed type of landing light which remains stationary relative to the airplane throughout the landing procedure, the illuminating effect of a beam pattern will vary with each of these phases. It is necessary that this beam pattern be so designed and the light suitably positioned

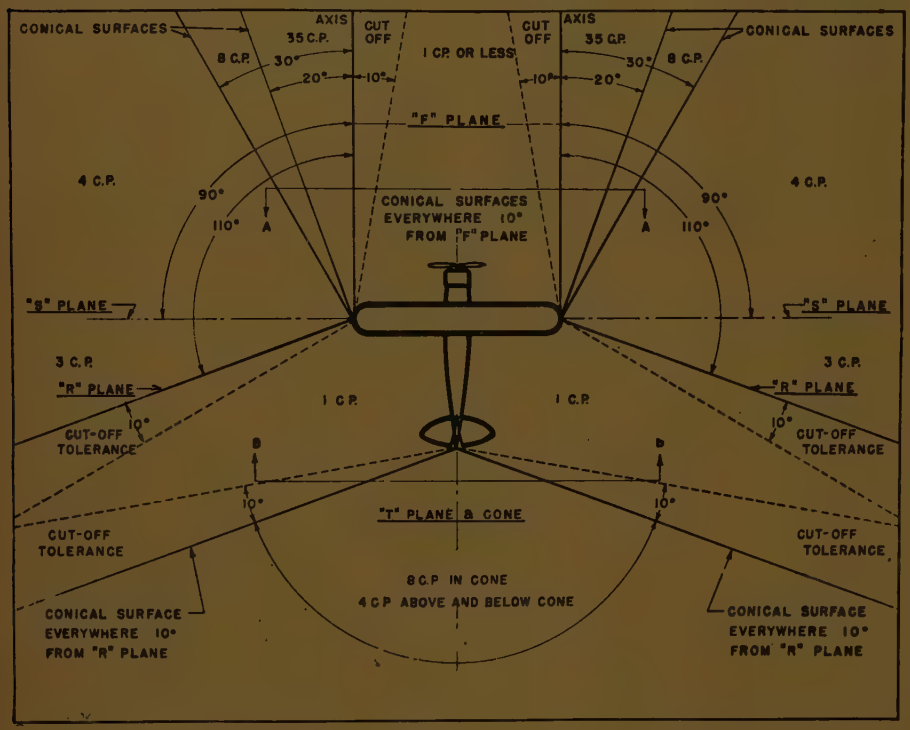


Figure 3. Angles and intensities of "airline forward" and "rear" position lights

It is also necessary to consider the effect of any position-lighting arrangement upon the passengers. Here again, the annoyance of a flashing light must be minimized, and the problem is complicated by the presence of polished wing surfaces.

New navigation-light fixtures should be designed so that the streamline of the airplane will be unaffected. It is most important that this streamline be kept inviolate as ice accumulation will not only reduce the utility of the lights, but will also seriously affect the aerodynamic efficiency of the wing.

Landing Lights

The general requirement for an aircraft landing light is that it furnish sufficient illumination in the area ahead of the airplane adequately to permit the pilot to guide the airplane to a safe landing. All three portions of a landing must be considered; namely, the approach to the field, the actual landing, and the subsequent taxiing to the parking space. During

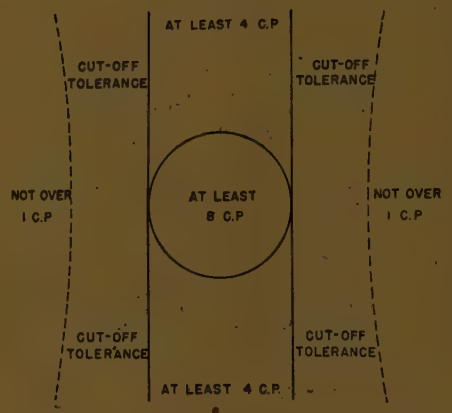


Figure 4. Showing section *B-B* of Figure 3

that sufficient illumination is obtained on the landing strip under all conditions.

There are in general three types of landing-light installations in use. The leading-edge-of-the-wing installation is the type most commonly used in commercial-airline aircraft (Figure 8). There are several disadvantages of this type of installation. The light lost by refraction through the curved lens is excessive, and the aerodynamic efficiency of the wing is affected. Furthermore, there is interference with the deicing of the leading edge of the wings. This type of installation is susceptible to damage from impact with objects such as birds with subsequent undesirable aerodynamic effects upon the airplane. The provision for bulb replacement is complicated by the necessity for installing sufficient fasteners on the hand-hole cover plates to meet the structural requirements for that portion of the airplane just aft of the leading edge of the wing. Consequently, the time required for a change of bulb is excessive.

The installation of the landing lights in the nose of the airplane, while not having the objections of interference with the deicing function or aerodynamic efficiency, does have a more objectionable effect of illuminating any haze or fog to such an extent as to interfere seriously with the pilot's vision from the cockpit.

A more recent type of landing-light installation is the retractable landing light (Figure 9). This light is usually mounted well under the wing and is controlled either hydraulically or by an electric motor. The bulb can either be illuminated automatically by means of a switch incorporated in the light assembly or can be illuminated in any position from the cockpit. The military services have standardized on the general requirements for electrically retractable landing lights for 12- and 24-volt d-c airplanes. The bulb is the sealed-beam type and is readily replaceable without the necessity of removing any wing structure. It is evident that this type of light does not have the objections common to the leading-edge and nose types; however, it has the objection of added mechanical complexity. This additional mechanism is another source of trouble, and should it fail to function satisfactorily, it will render the landing light inoperative. The Civil Aeronautics Administration, recognizing the various forms in which the landing-light installations might appear, has framed its requirements as follows.

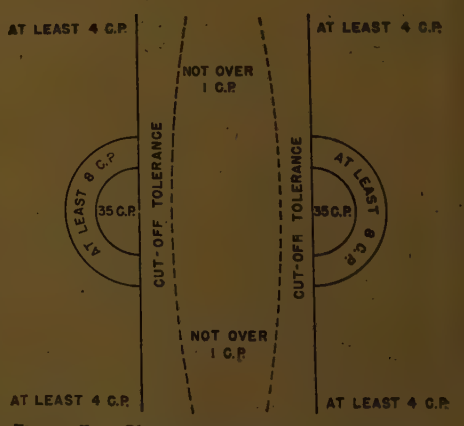


Figure 5. Showing section *A-A* of Figure 3

"Electric landing lights shall be so installed on multiengine aircraft that at least one should not be less than ten feet to the right or left of the first pilot's seat and be beyond the swept disk of the outermost propeller. On single-engine aircraft such lights shall be so installed that no visible portion of the swept disk of the propeller, if of the tractor type, is illuminated thereby. Individual switches for each light shall be provided in the pilots' compartment." It is a further CAA requirement that two lights be provided on any air carrier of passengers licensed for night flying.

To sum up the landing-light situation, present-day airplanes having the conventional tail-wheel type of landing gear do not provide suitable illumination for all three phases of airplane maneuvering involved in the landing. As a result the lights are adjusted so as to favor the illumination of the landing strip during the approach and landing position with the result that the light available for taxiing is very poor. It has been suggested that landing lights be furnished with double filaments similar to the type employed in automobiles so that taxiing might be accomplished under more favorable lighting conditions. Another solution to this problem would be properly to proportion the beam pattern from the landing light so that a sufficient amount of light is available at the various attitudes of the airplane. This can only be done, however, at the expense of the light intensity at any one working condition. In view of the compromises currently being accepted in regard to bulb life and lumens per watt output, it would be undesirable to adopt any system which does not fully utilize the lumen output of the lamps.

A problem of aircraft lighting which is seldom appreciated by anyone but the airline operator is the lamp burnout and replacement problem. This problem is particularly acute for the position and landing lights for the following reasons. An air carrier for passengers cannot be given flight clearance without the full compliment of position and landing lights as specified by the CAA. Landing lights, which are lighted just prior to the aircraft's departure from the loading ramp, and in some cases not until the aircraft is ready for take-off, usually fail when the lamp is switched on. In the case of both the position lights and the landing lights the transition, from the airplane's condition at rest with engines not running to the excessive vibration experienced when engines are being ground run and the airplane is taxiing, is sufficient to produce failure in a bulb which has approached the end of its service life. In most cases, therefore, the light-bulb failures occur at the most critical time and result in what is known as a station delay or an interrupted flight. These delays or interrupted flights are the "headaches" of airline operation.

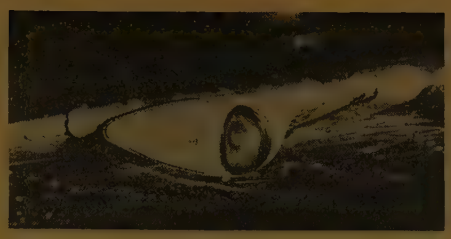


Figure 7. Installation of approved type of wing-tip position light



Figure 8. Leading-edge-of-the-wing type of landing light

Considering the life expectancy characteristic of the incandescent lamp, the best that one can hope for, if a periodic replacement program is put into effect, is to reduce percentagewise the number of delays and interruptions. This procedure can never eliminate them. The economy of such a program must be balanced against the intangible cost of a delay or interrupted flight. A possible solution of this difficulty for the position lights is the use of lamps having double filaments of different structural characteristics so as to provide a lamp for which only one filament would burn out at a time. This would have the advantage of providing a light of reduced intensity in case the burnout of one of the filaments occurred in flight. It is obvious that such a system could not be applied to the landing-light bulb.

Auxiliary Lights

In addition to the position and landing lights various other auxiliary lights for lighting some portion of the aircraft exterior are being used. The pilot's desire to see the leading edge of the wing at night during icing conditions has resulted in the installation of lights in either the fuselage or the outboard nacelles which floodlight the leading edges of the wings. Lights which are being used for this purpose are the type manufactured as automobile spotlights. These lights are provided with refracting lenses to provide the particular light pattern required for even distribution along the entire length of the wing. The installation of these lights has met with the approval of the CAA. They are satisfying a need which will exist until more positive and automatic wing deicing equipment is available. It is interesting to note that these wing ice lights are also used as an aid to taxiing on the ground.

Lights have been installed inside of the engine nacelles which house the landing gear in order to floodlight the gear so that the pilot may check its position in flight at night. The installation of lights adjacent to the cargo doors has been proposed so as to provide sufficient illumination for cargo handling.

Interior Lighting

Cabin Lighting

The problem of cabin lighting in a commercial airliner is quite similar to that of



Figure 6. Dual flashing taillight installation



Figure 9. Under-the-wing-type hydraulically retractable landing light

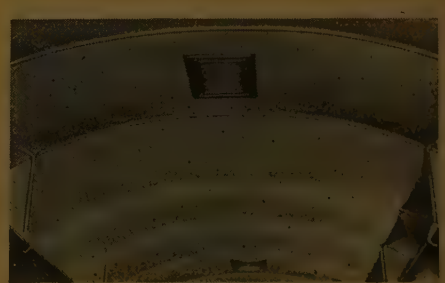


Figure 10. Cabin dome-light installation in a DC-3 airplane

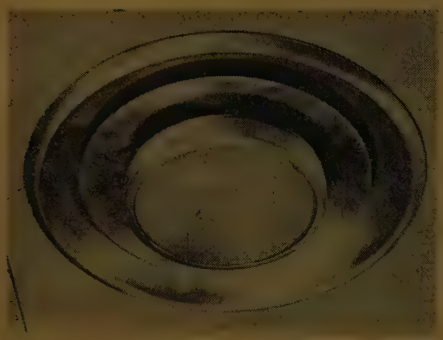


Figure 11. Combination dome light and ventilator outlet—Douglas *DC*-4 airplane

the modern deluxe coach or railway car. Generally speaking, it is necessary to provide two types of illumination; namely general lighting and individual-seat lighting. The general illumination for cabins commonly consists of a number of dome-light fixtures extending along the center of the cabin ceiling and suitably spaced to provide a uniform intensity of illumination at the seat level (Figure 10). These dome-like fixtures usually consist of one or two incandescent light bulbs of 21 to 32 candlepower, each covered by a diffusing light cover made of glass or a lighter plastic equivalent. Care must be taken in the selection of the plastic compound for this purpose inasmuch as the limited space provided for the bulbs results in fairly high temperatures. The usual thermoplastic materials will sag and warp and eventually present a very unattractive appearance. Figure 11 illustrates a Douglas Aircraft Company design which places the light inside of a ventilation duct thereby obtaining better cooling of the lamp enclosure.

Some means must be provided for lowering the intensity of the dome lights during night flight so that passengers desiring to sleep will not be annoyed. The dimmed lights must provide enough illumination for the stewardess or passenger who may desire to move about the airplane. A better solution of this problem is the installation of aisle lights which may be turned on when the dome lights are extinguished. In this manner the illumination is applied where it is needed with a minimum of discomfort to reclining passengers. The aisle lights are commonly installed at the base of the individual seats and so positioned that a uniform lighting of the aisle is obtained.

A particular problem of cabin lighting at night exists at the vicinity of the main cabin door where passengers leaving or boarding the airplane must be checked in and out by the stewardess. Since it is not customary to wall off that section of the cabin containing the seats from the entrance door, the light in the vicinity of the door must be so designed that its cutoff in the forward direction will not permit it to strike the rearmost reclining-passengers' heads. Furthermore, the brightness of the light-cover glass must be kept to a minimum when observed from the rearmost seat. While it is obvious that these



Figure 12. Individual seat light installations—Douglas *DC*-3 airplane

requirements may be simply satisfied by some sort of shade, the height of the cabin ceiling and the undesirability of installing any projecting surfaces make this method of solution impractical.

The basic requirement of the individual seat light is that it illuminate the normal reading area in front of the passenger without annoyance to any other passengers. The normal positions and movements of adjacent passengers should not interfere with the light supplied to the seat for which it is intended. Individual-seat-light installations usually take the form of an incandescent lamp totally enclosed in a metal box with the exception of a small opening suitably designed to provide the required beam pattern (Figure 12). The switches for controlling these lights must be easily reached from their respective passenger seats. The use of lenses for the purpose of improving the efficiency of the lighting system is impractical from the maintenance viewpoint inasmuch as the type and focal length necessary for this application requires the maintenance of close tolerances between these two parts. Furthermore, the use of lenses presents an additional cleaning problem.

The individual lighting installation is further complicated on airplanes with sleeping accommodations inasmuch as suitable lighting must be provided in the upper berths. The problem of interference is minimized inasmuch as the individual compartments are partitioned or curtained; however, care must be taken that upper and lower berth lights in the same compartment do not provide stray illumination to one another.

In addition to the general cabin lighting, there are various compartments requiring lighting; namely, dressing rooms, toilets, coat closets, and buffets. Each of these compartments presents its particular problems. The general type of dome light is usually employed with the exception of the dressing rooms where additional lights are provided adjacent to the mirrors (Figure 13).

Up to the present time fluorescent cabin lighting has not made its appearance in the commercial airplane. In view of the above requirements, it appears that an application exists for the fluorescent type of lighting for supplying general cabin and compartment illumination. In the application of fluorescent lighting to aircraft, the following factors must be evaluated:

1. What will be the difference in weight over that of an incandescent bulb type of installation providing the same level of illumination?
2. How will the service life and cost of operation compare with the existing system of incandescent lighting?
3. Does the improved appearance of the fluorescent type of installation warrant penalties to be taken on either the weight or maintenance items?

Cockpit Lighting

If one were to ask what the most important lighting application is in an airplane, the answer would be universal in its agreement on the instrument panel lighting. The importance of proper panel lighting and its contribution to the safe operation of the airplane has long been recognized and a number of different types of lighting systems developed.

The general problem of instrument-panel lighting can be stated as the application of a controlled, uniform intensity of shadowless illumination over all instrument dials which must be observed by the pilot during the routine operation of the airplane. Furthermore, this illumination must be so accomplished that there are no extraneous reflections of light visible to the pilots from instrument cover glasses, windshields, or other reflecting surfaces. The instrument-lights' function has been similarly described by the CAA in their regulation CAR 04.5826 which reads, "Instrument lights shall be so installed as to provide sufficient illumination to make all flight instruments easily readable and shall be equipped with rheostat control for diming unless it can be shown that a nondimming light is satisfactory."

There are in general three types of instrument-light installations. They are:

1. Incandescent bulb floodlighting of the instrument panel.

2. Individual (built in) ring lights on each instrument.

3. Fluorescent floodlighting of the instrument panel.

Each of the above methods has its advantages and disadvantages of which the following are the more important. In floodlighting the panel by incandescent light bulbs, the following problems are outstanding:

1. Reflections from instrument cover glasses and windshields.

2. Even distribution and shadowless illumination of all the instruments.

3. The difficulty of locating the various lighting fixtures in the airplane to accomplish the first two objects.

The individual ring lighting of instruments solves the problem of extraneous reflections and of securing uniform and shadowless illumination. However, the chief disadvantage of this type of lighting is the penalty which must be taken in the installation as each instrument must have a wire connection, and these wires must be located in back of the instrument panel which is one of the more congested spots on the airplane.

The use of fluorescent lights for instrument lighting has been given considerable impetus by the military services for whom the dark-cockpit type of night flying is an absolute necessity because of tactical reasons. The principle of fluorescent lighting has been widely demonstrated to the general public by theaters which have used this medium for staging spectacular costume changes. It is based upon the action of near-ultraviolet radiation on fluorescent paints. The wave length of this radiation is in the band from 3,000 to 4,000 angstroms and consequently has no detrimental effect to the eyes or skin. The efficiency of the fluorescent lamp is approximately two to three times that of the equivalent wattage of incandescent lamp. The history of the instrument-panel fluorescent lighting goes back to the year 1934 when an installation of a cold-cathode argon lamp was used with a cobalt-blue glass filter to flood the instrument panel with near-ultraviolet radiation (Figure 14). The instruments were coated with a radium base paint which reacted quite readily to these lights and produced a legible instrument indication without any extraneous illumination. The limitations of the argon lamp as a source of ultraviolet light became apparent as the size of instrument panels and the distance which these lights had to be mounted from it became greater.

A series of 4- and 6-watt 118-volt a-c fluorescent lamps were designed in conjunction with the United States Army Air Corps, who pioneered in their development. The design of these lamps is unique in that they contain a fluorescent coating on the inside of the glass envelope which transposes the ultraviolet radiation



Figure 13. Mirror lights—Douglas *DC-4* airplane

of the ionized mercury-vapor gas into both visible and near-ultraviolet radiation. The lamp is provided with a rotable shutter and a blue glass filter which permits some control in the amount of near-ultraviolet radiation output and permits the use of visible light for reading of maps and charts in the cockpit. These lamps require a power source of 118 volts for their operation as well as a reactor (ballast) and a starting switch similar to that employed in the commercial fluorescent lighting equipment.

Within the past year a 24-volt d-c mercury-vapor bulb has been made available. The same basic principles apply with the exception of the size and shape of the bulb which resembles an automobile headlight lamp and is enclosed in a fixture provided with a slotted face for permitting either dark lighting or visible lighting of the instrument panel. The use of the 24-volt lamps in aircraft having 24-volt d-c electrical systems has abolished the need for inverters formerly required for the 118-volt equipment.

In conjunction with the design of the fluorescent lighting equipment just described, the military services have sponsored the use of fluorescent-paint

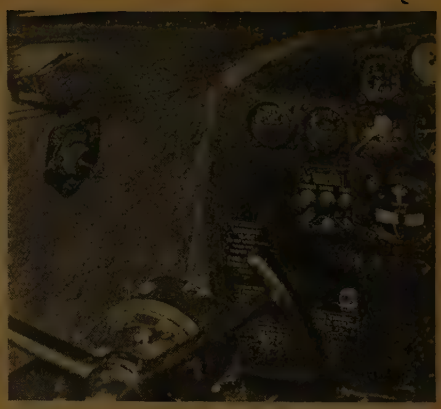


Figure 14. Instrument-panel light installation —*DC-3* airplane

materials which are activated by the near-ultraviolet radiation but which extinguish immediately upon removal of the radiation. The development of fluorescent-paint material has progressed to the stage where a wide range of colors are available which will fluoresce with approximately the same color as they appear in daylight. This property is advantageous in order to preserve the utility of the instrument limit markers used to designate various safe minimum and maximum values of engine pressures, temperatures, and airspeed.

Independent studies made by a number of physiologists have demonstrated that the ideal color for instrument illumination is in the red spectrum, the reason being that the red waveleng hs have very little effect upon the dark-adapted eye. As a result of these findings, incandescent-light fixtures have been equipped with red glass filters for floodlighting panels with red light for night flying where a minimum of interference with the pilot's ability to discern objects through the windshield is required.

As in most lighting applications, particularly those involving colored lights, the personal element is an important factor. As an example, one can cite the attitude of the commercial airline pilots to the argon lighting system which has been in use for the past nine years. There are about as many pilots who do not use this form of lighting because they personally do not like it as there are pilots who would not be without it. The same reactions have been recorded by the physiologists who have noted the personal opinions of the various subjects testing the red-light installation. Much to the surprise of some of these subjects, the tests showed that while they disliked the colored lighting, their visual ability to see objects out of the cockpit had been improved over their previous ability with their customary and preferred type of illumination.

Some form of glareless general cockpit

illumination is necessary in addition to various spotlights provided for illuminating the pilot's controls, switches, valves, etc. It is common practice for most pilots to fly with a brightly lighted cockpit when cruising on flights of long duration. Apparently this practice has been found less fatiguing than flying with a darkened cockpit with only localized lighting for illumination. The ability to flood the cockpit with general illumination is a desirable safety measure when flying through thunderstorms in the presence of lightning. It has been determined from a number of recorded cases in which pilots were momentarily blinded by lightning flashes that the duration of their disability was generally related to the intensity of cockpit illumination prior to the discharge.

In addition to the problem of illuminating the instrument panel for the pilots, there is the problem of satisfactorily illuminating the work areas for the flight engineer, navigator, and radio man in the larger airplanes having provisions for such personnel. The lighting provisions for each of these crew members usually require the illumination of an auxiliary instrument panel mounted in front of the observer plus a local spotlight for illuminating his table area. All lighting for the individual crew member must be carefully arranged so as not to interfere with the other crew members. The various lighted work areas of the nonpiloting crew members must be shielded from the pilots' compartment and it is customary to drape a curtain between the pilots and the remainder of the flight-crew compartment. It is also necessary to provide a suitable light shield for the navigator's use when sighting through the ship's astrodome. In general, the location of the various light sources and curtains must be determined from a full-scale mock-up of the crew compartment. It is essential that the installation of all glass reflecting surfaces be duplicated in the construction of the mock-up. Only in this manner can the multiple reflections obtained from these windows be detected and eliminated.

Conclusion

The problems associated with the lighting of a commercial transport airplane are many and varied, and current practice leaves much to be desired. Generally speaking, there is need for the development of colored position-light sources of greater intensity and better efficiency. Such developments should seek a reduction in size and weight of the installation and be designed to fit more closely the aerodynamic streamline of the airplane.

In the field of aircraft landing lights the development of a light which will meet all the requirements associated with the landing and ground running of the airplane without increasing the weight of the installation is needed. The present life of the position light and particularly the landing-light bulb is exacting too great a penalty from the airline operator in view of the coincidence of lamp failures with the time of aircraft departure.

Perhaps the greatest immediate change which can be expected in the post war commercial airliner will be in the realm of cabin lighting. This opinion is based upon the unexploited application of fluorescent lighting to aircraft cabin interiors. Light installations of more pleasing appearance and producing a higher intensity of illumination for the individual passenger at less cost in weight and energy are needed.

The flight crew personnel can look forward to improved cockpit lighting installations as a result of the experience which is constantly being accrued by the military services in the laboratory as well as in the field.

No paper on aircraft lighting would be complete without due acknowledgement to those agencies which are actively engaged in furthering commercial aircraft lighting developments. These are the aircraft lighting committee of the Air Transport Association, the committee on aviation lighting of the Illuminating Engineering Society, the technical development division of the Civil Aeronautics Administration, and the United States Bureau of Standards. These agencies are mutually co-operative and will provide the necessary guiding hand to the future development of commercial aircraft lighting.

Fusing Practices on Distribution Systems—II

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A PAPER on fusing practices on distribution systems operating at five kilovolts and below was presented at the winter technical meeting.[1] It discussed the results of a questionnaire which had been submitted to about 200 operating companies and presented the data in graphical form. The data of the present paper were gathered on systems operating between 5 and 15 kv. Wherever possible, the form of the previous paper has been followed in order to permit ready comparison.

Distribution Transformer Fusing

In answer to the question, "Why do you fuse distribution transformers?" it was found that 86 per cent of the companies fuse to protect the system from the transformer, 61 per cent fuse to protect the transformer, 18 per cent fuse to indicate the location of faults, and 17 per cent fuse as an isolating means. The total of these percentages is more than 100 per cent, because several companies expect the fuse cutouts to perform more than one function.

The primary windings of distribution transformers are fused by the use of enclosed indicating cutouts by 34 per cent of the companies. The enclosed nonindicating cutout is preferred by 12 per cent of the companies, and 54 per cent expressed a preference for the open dropout type. Perhaps the large preference for the open type is caused by the fact that the insulation is such as to put a grounded mounting bracket between the terminals of the fuse. At this higher voltage the dropout feature is almost a necessity in order to prevent voltage from appearing across exposed organic insulation of a blown fuse.

The use of repeating fused cutouts on distribution transformers is very limited. This probably is brought about by the relatively high cost of this type of cutout and by the fact that almost all internal transformer faults are of a permanent nature. However, nine per cent of the companies reported some use of repeating cutouts for transformer fusing. This agrees with the eight per cent found on systems below five kilovolts.

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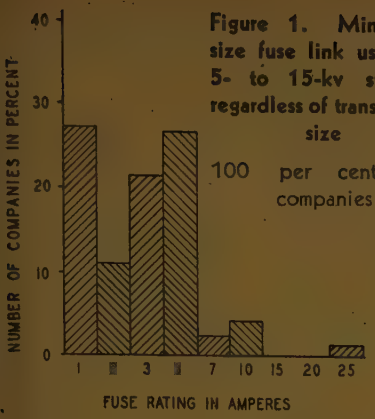


Figure 1. Minimum-size fuse link used on 5- to 15-kv systems regardless of transformer size

100 per cent=104 companies

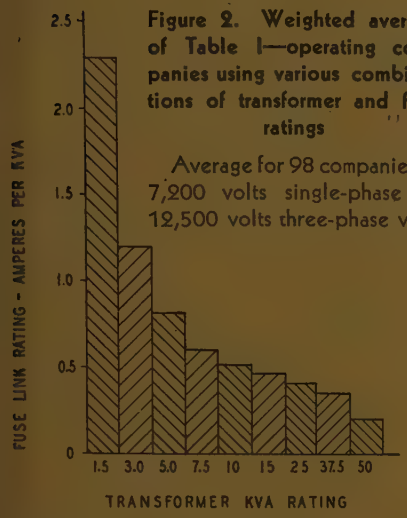


Figure 2. Weighted average of Table I—operating companies using various combinations of transformer and fuse ratings

Average for 98 companies 7,200 volts single-phase or 12,500 volts three-phase wye

As in the previous questionnaire, the companies were asked the extent of the use of cutouts at locations where the short-circuit current exceeds the published interrupting rating. It was found that the policy of 37 per cent of the companies is to use cutouts at locations where the published interrupting ratings might be exceeded.

Transformer Fuse Ratings

The rating of the primary fuse which is chosen for a distribution transformer is usually correlated with the kilovolt-ampere rating of the transformer. In the case of smaller transformers, usually below five kilovolt-amperes, other factors predominate. Consequently, the question was asked as to the minimum-size fuse link used on the system and the factors controlling its choice. The three factors most frequently mentioned in the order of their predominance were mechanical strength, lightning, and transformer rating. Figure 1 illustrates the practice as to minimum fuse rating and shows graphically the number of companies in per cent using a given size. The range of values is about the same as that found for lower-voltage systems except that a larger number use the one-ampere link.

Table I is a transformer fusing chart showing the percentage of operating companies using the various combinations of transformer and fuse ratings. The most popular link for each transformer rating has been pointed out by the bold-faced type in the table. This table applies to 6,600–7,600-volt single-phase systems and 11,000 to 13,800 volts three-phase wye systems. It is interesting to note that the plurality of customers use a five-ampere link as a minimum size for all transformers up to 15 kva. A weighted average of Table I is graphically presented in Figure 2 which shows the fuse link rating in amperes per kilovolt-ampere plotted against the transformer kilovolt-ampere rating. It will be noted that the fusing ratio varies widely, a much higher fusing ratio being used for the smaller transformers. The data in Table I have been plotted again in Figure 3. This table shows the number of companies in per cent using a given fusing ratio, the fusing ratio being the link rating divided by the full-load current of the transformer. The data show even less uniformity in fusing practice than is found for transformers operating below five kilovolts.

Table II applies to systems operating at voltages ranging from 6,600 to 7,600 volts three-phase delta. Three-phase wye systems from 6,600 to 7,600 volts are shown in Table III. Single-phase systems operating between 11,000 and 13,800 volts are found in Table IV. Table V applies to systems operating in three-phase delta between 11,000 and 13,800

Table I. Operating Companies Using Various Combinations of Transformer and Fuse Rating

7,200 Volts Single-Phase or 12,500 Volts Three-Phase Wye
100 Per Cent=98 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes										
	1	2	3	5	7	10	15	20	25	30	40
	Number of Companies in Per Cent										
1.5	22.8		17	19.3		**35.3**	1.1		4.5		
3	20.0		14.4	20.0		**40.0**	1.1		4.5		
5		5.3		8.5	31.9	**44.7**		2.1		7.5	
7.5		3.0		8.0	23.0	**46.0**		7.0		13.0	
10			7.1		28.6	**42.8**		3.6	14.3		3.6
15			2.2		4.3	**55.4**		8.7	17.4	7.6	3.3
25				2.1		8.3	24.7	**42.2**	11.3	6.2	3.1
37.5					2.2		3.3	**42.4**	29.3	8.7	7.6
50						1.6		14.8	**50.8**	14.8	9.8

Table II. Operating Companies Using Various Combinations of Transformer and Fuse Ratings

7,200 Volts Three-Phase Delta; 100 Per Cent=33 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes												
	1	2	3	5	7	10	15	20	25	30	40	50	65
	Number of Companies in Per Cent												
1.5	3.9		11.5	**42.3**		34.6			7.7				
3	3.6		3.6	28.5	**53.5**		3.6		7.2				
5		3.0		18.2	**42.5**	21.2		12.1		3.0			
7.5		3.2		3.2	**35.6**		6.5	29.0		19.3	3.2		
10						16.1			**42.0**		16.1	16.1	9.7
15				3.0		6.1	**30.3**		18.2	21.2		9.1	12.1
25									**19.4**		16.1	12.9	12.9
37.5										**20.0**	20.0	6.7	13.3

Table III. Operating Companies Using Various Combinations of Transformer and Fuse Rating

7,200 Volts Three-Phase Wye; 100 Per Cent=21 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes												
	1	2	3	5	7	10	15	20	25	30	40	50	65
	Number of Companies in Per Cent												
1.5	7.1		14.3		26.6	**37.7**		7.2		7.1			
3		7.1			28.6	**57.2**		7.1					
5		6.3				**62.3**		18.9		12.5			
7.5				6.3		**50.0**	18.7		18.7		6.3		
10				6.2		**31.3**		6.2		**31.3**		25.0	
15						14.3			28.6		**42.8**	9.5	3.8
25											**40.0**	13.3	20.0
37.5											**26.6**	6.7	13.3

volts. The data of Tables II through V could be shown in graphic form as was Table I in Figures 2 and 3 and would show the same general trend.

All companies replying use primary fused cutouts. Reference to Figure 3 will show that the fusing ratios are such that in many cases low-impedance secondary faults near the transformers will be cleared by the primary fuses. However, secondary fuses are used by 18 per cent of the companies, as contrasted with 12 per cent for systems operating below five kilovolts.

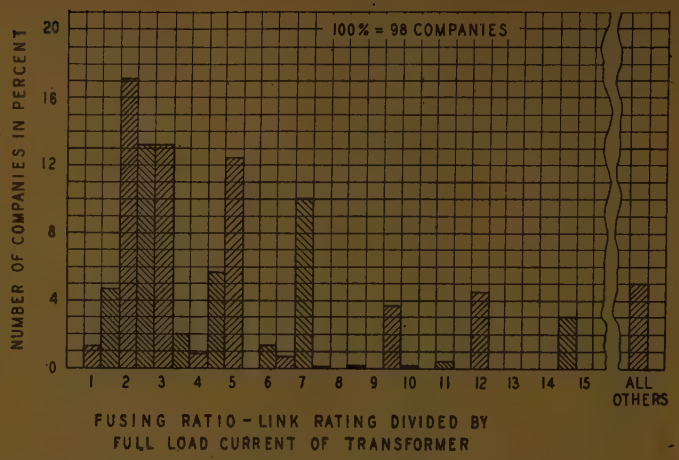


Figure 3. Weighted average fusing ratio for all ratings of transformers plotted from data of Table I

Line Sectionalizing

It was found that 88 per cent of the companies make use of fused cutouts for line sectionalizing. Of these, 66 per cent use them on the main feeders, 87 per cent on the three-phase branch feeders and 95 per cent on single-phase laterals. The repeating cutout is most frequently found on the main feeder where 67 per cent are of this type. The three-phase branches are fused by repeating cutouts in 52 per cent of the cases, and this figure drops to 39 per cent on single-phase laterals. About 74 per cent of all cutouts used for line sectionalizing are of the open type. This contrasts sharply with the practice found on systems operating below five kilovolts, where 70 per cent are of the enclosed type. The rather wide variation in fusing practice for line sectionalizing is illustrated by Figure 4. This shows the number of companies in per cent using a given number of line-sectionalizing fuses in series. This figure shows that 63 per cent of the companies consider the use of more than three sectionalizing fuses in series to be impractical.

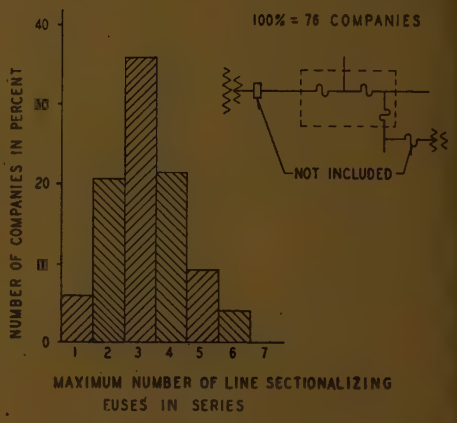


Figure 4 (below). Line-sectionalizing practice employed by 76 companies

Conclusions

In general, the data applying to systems above five kilovolts agree closely with those for systems below five kilovolts. Perhaps one reason for this is the fact that the same companies were questioned in each case. There are a few discrepancies, such as the preference for open cutouts at the higher voltage, and these are easily understood from a consideration of the factors involved. There appears to be a trend towards larger fuse sizes for use with distribution transformers, which the authors consider to be very desirable. The high percentage of the repeating fuses for sectionalizing service is indicative of a trend toward automatic reclosing, but it is the authors' belief that the small reclosing breaker eventually will supplant repeating fuses for line protection.

Discussion with operating engineers reveals that many have come to the conclusion that the use of larger fuse links leads to reduced operating expense without any actual increase in the rate of transformer failure. The larger link while reducing unnecessary outages does not change appreciably the time required to disconnect a faulted transformer from the circuit. As no fuse link can give adequate thermal protection to a transformer, little if anything is lost by employing larger links. It is hoped that further study of this problem will lead to additional operating data which will allow quantitative interpretation of the merit of increasing the size of fuse link used on transformers.

Table IV. Operating Companies Using Various Combinations of Transformer and Fuse Rating

13,200 Volts Single-Phase; 100 Per Cent = 44 Companies

Single-Phase Transformer Rating—Kva	Fuse Link Rating—Amperes								
	1	2	3	5	7	10	15	20	25
	Number of Companies in Per Cent								
1.5	22.6		9.1	9.1		41.0		18.2	
3	33.4		12.1	9.1		30.3	3.0	12.1	
5	34.1		9.1	20.4		25.0	2.3	9.1	
7.5	9.8		24.4	19.5		34.1	2.4	9.8	
10	7.1		7.1	21.5		21.5	7.1	35.7	
15			11.4	31.8		45.4	6.8	4.6	
25			5.0	12.5		50.0	12.5	17.5	2.5
37.5				4.9		31.7	17.1	34.2	7.8	2.4	2.4
50						9.8	7.3	48.8	24.4	7.3	2.4

Table V. Operating Companies Using Various Combinations of Transformer and Fuse Rating

13,200 Volts Three-Phase Delta; 100 Per Cent = 36 Companies

Single-Phase Transformer Rating—Kva (Multiply by 3 for Bank Kva)	Fuse Link Rating—Amperes												
	1	2	3	5	7	10	15	20	25	30	40	50	65
	Number of Companies in Per Cent												
1.5	15.4	11.5	23.1	46.2		3.8							
3	6.5	9.7	25.8	51.5		6.5							
5	3.0	6.1	21.2	45.4		18.2	6.1						
7.5	5.6	5.6	50.0	11.0		25.0			2.8				
10	3.2	3.2	25.9	16.1		19.3	25.9	3.2	3.2				
15	2.8	27.7	5.6	22.2		22.2	16.7		2.8				
25		2.9	5.9	38.3		5.9	17.6	14.7	11.8			2.9	
37.5			8.6	17.1		22.9	17.1		5.7	11.4	11.4	2.9	2.9

Reference

1. Fusing Practice on Distribution Systems—I, John S. Parsons, J. M. Wallace. AIEE Transactions, volume 63, 1944, March section, pages 89–91.

Electronic Regulator for Arc Furnaces

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Synopsis: With the increased use of arc furnaces in the production of alloy steel, new interest has been aroused in both old and new methods of automatic regulation of electrode position. The regulator described in this paper is an electronic type which controls electrode position as determined by response to current in the electrode and voltage between the electrode and the furnace shell. Features and performance of an installation for a furnace having hydraulic control are illustrated.

THE power input to arc furnaces has always been controlled by automatic adjustment of electrode position. Figure 1a shows a typical installation of an arc furnace which employs hydraulic electrode positioning. In this type of furnace air up-push is used to counterbalance the weight of the electrode arm. Hydraulic means, controlled by a small motor, is employed to lower or raise the arm.

In the operation of the furnace, electrode adjustment is necessary because, as the charge melts down, the electrode must be lowered to maintain the arc. The scrap, as it melts, frequently falls against the electrodes, causing a short circuit and making withdrawal necessary. Adjustment of the electrodes is also necessary to allow for normal consumption of the graphite of which they are made.

Early methods of arc-furnace control consisted of simple current-operated devices. The electrode current in each phase was supplied from a current transformer to the coil of the regulator element. The pull of the coil was then balanced against a spring and the operation of the regulator contacts caused the electrode motor contactors to close the motor armature circuit in a manner to either raise or lower the electrode as the current deviated from a predetermined value. This method of regulation was found generally unsatisfactory, chiefly because the restraining force of the spring had a constant value and other factors were not taken into account.

A better method was then developed using another coil in place of the spring. Across this coil was impressed the arc voltage. As is well known, the voltage drop across the arc will increase as the current is decreased and vice versa. It can be readily understood that, when the varying pull of the coil or other regulating device which is proportional to the electrode current is balanced against a restraining force which is proportional to the arc voltage, the utmost sensitivity to changes in arc length is obtained in the regulator.

This principle of regulation has been in use for a period of about 15 years and has proved the best so far devised. For further improvement it seemed necessary to look to the actual control of voltage impressed across the electrode control motors, which for many years has been a constant potential d-c reversing control combined with dynamic braking. Figure 1b shows a view of the electrode motors used for controlling the electrodes of the arc furnace shown in Figure 1a. The motor in this case is one-half-horsepower frame size.

Quite recently electronic motor control has been developed and has proved successful on many applications where wide range of speed control and quick response is desired. It is possible to obtain many combinations of acceleration and speed range with a minimum of apparatus and almost instantaneous response. The means for varying motor speed is different from the older method of varying the field of the motor and holding a constant potential on the armature. Instead, the field is held constant and the armature voltage is varied to give the desired speed. The use of electronic regulation and control of the type described in this paper, for electrode motors, eliminates moving parts in the electrical equipment up to the motor. At the same time it provides the necessary sensitivity and high speed of response.

The electronic regulator includes a number of electronic tubes which take the place of relays and contactors to control the input to the furnace electrode motors. The power supplied to the regulator is taken from the available a-c line and no direct voltage is required from any separate source. The grid to cathode circuit of the vacuum tubes constitutes the input of the device, while the output is obtained from the thyratron-tube circuit. The principal grid voltages for the vacuum regulating tube include:

1. A control voltage which serves as a reference against which the quantity to be regulated is compared.
2. A control voltage which is proportional to the quantity to be regulated.
3. A self-biasing voltage connected in series with the cathode.

For a high degree of accuracy, the various grid voltages should have complete control over the regulating tube without causing the tube to saturate



Figure 1a. Typical arc furnace having electrodes arranged for hydraulic positioning



Figure 1b. Electrode control motors for the furnace shown in Figure 1a

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J. E. Reilly and C. E. Valentine are with the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

The authors acknowledge the co-operation and contributions of C. C. Levy of the industry engineering department, Westinghouse Electric and Manufacturing Company, in the development of this regulator, and also the co-operation of the Hydro Arc Furnace Company and the Electrocast Steel Foundry for the facilities placed at their disposal in making tests.

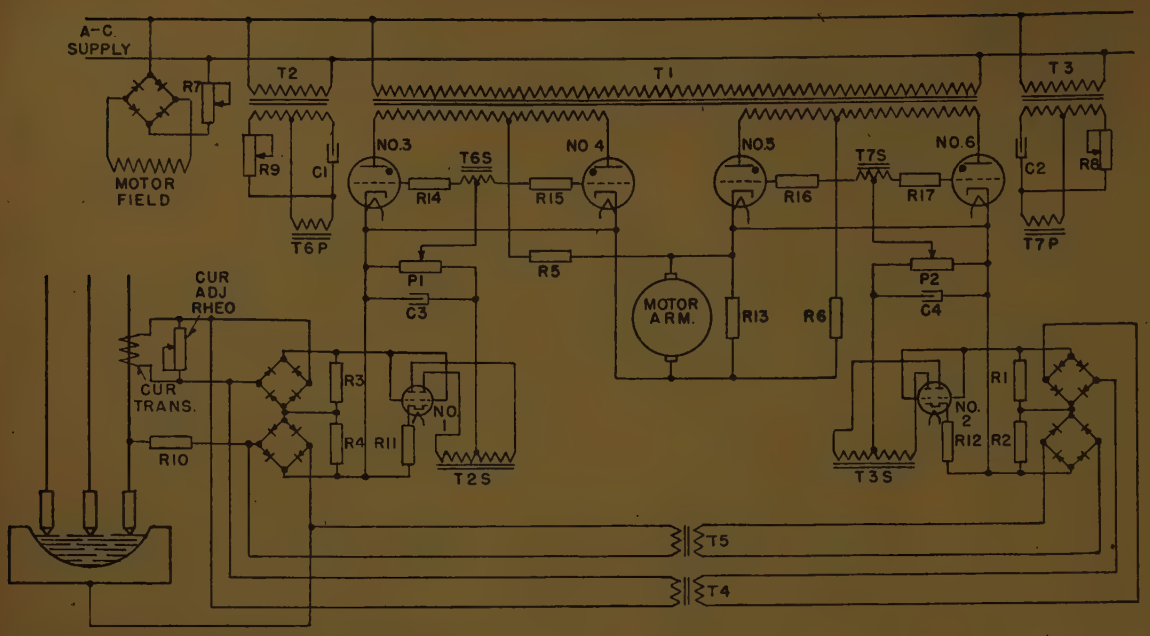


Figure 2. Simplified schematic diagram of arc-furnace regulating system

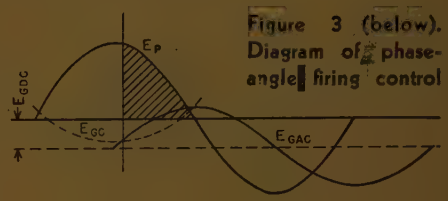


Figure 3 (below). Diagram of phase-angle firing control

because of the grid swinging positive. The self-biasing resistor is designed so that the regulating tube will be working along the straight line of its saturation curve similar to that of a class *A* amplifier. This results in a high degree of amplification since an extremely small change of the grid voltage is ample to change the output of the regulating tube over a wide range.

The speed of response of this type of regulating element is almost instantaneous. There is practically a complete absence of any retarding quantity such as inductance which might delay changes of current flow within the element.

Figure 2 shows an elementary schematic diagram of the regulating system. To simplify the diagram, certain details including grid balancing circuits, have been omitted. The grid control voltage of the regulating tubes, proportional to the electrode current, is taken from two Rectox rectifiers, which are connected to a current transformer in the electrode circuit. The grid control voltage, proportional to the arc potential, is taken in a similar manner from the voltage between each electrode and the shell of the furnace. In practice, when the furnace transformer breaker is closed, voltage is applied to the potential circuit which causes the output of tube number 1 to decrease. This in turn decreases the drop across potentiometer *P*1, supplying negative grid direct voltage to tubes numbers 3 and 4. When the negative grid voltage is decreased, tubes numbers 3 and 4 break down and start conducting, supplying full-wave rectified voltage to the electrode motor in the direction to lower the electrodes.

When the first electrode strikes the metal, the voltage drops to zero causing the voltage to be removed from the potential circuit, thereby allowing tube number 1 to increase its output, consequently increasing the negative grid voltage on tubes numbers 4 and 5, thereby blocking the conduction of current through these tubes. When a second electrode strikes the metal, current flows causing a voltage to appear in the current circuit which will place a negative grid voltage on tube number 2, thereby decreasing its output. Tubes numbers 2, 5, and 6 and potentiometer *P*2 will operate in the same manner as tubes numbers 1, 3, and 4 and potentiometer *P*1 did for the lowering operation. A voltage will then be impressed on the armature circuit in the direction to raise the electrodes thus establishing an arc. As the arc is lengthened, its voltage increases and the current decreases until a balance is established between the potential and current grid control voltages. The third electrode is controlled in a similar manner.

As the charge melts and flows down, or as the electrodes burn off, or the scrap falls against the electrodes, the balance between the control grid voltages is continually being disturbed. When this takes place, the two regulating tubes change the voltage output from the thyratron tubes that supply the electrode-motor armature power.

The variable voltage for the motors is obtained by superimposing an alternating voltage wave on the grid direct voltage of the main thyratron tubes. By displacing the a-c wave 90 degrees lag with respect to the thyratron anode voltage, the a-c wave will cut the critical grid voltage line at different points as shown by Figure 3, as the direct grid voltage of the power tubes is either raised or lowered. This allows the thyratron to conduct for varying portions of the half cycle.

A wide range of current adjustment is obtained by means of a rheostat shunting

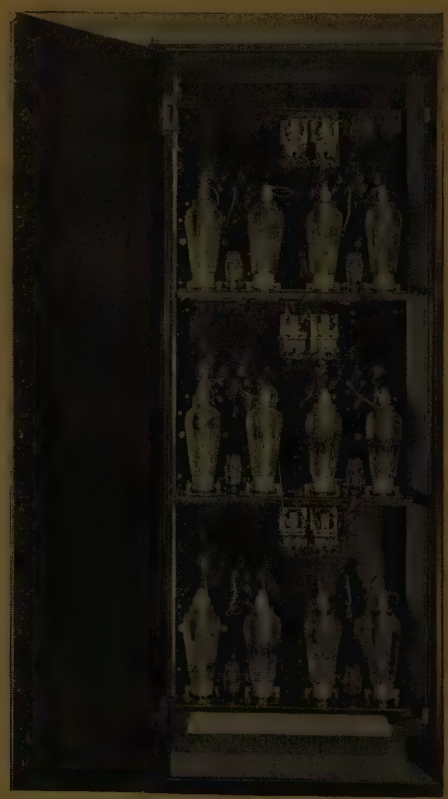


Figure 4. Metal-enclosed electronic regulator unit

the current transformer. This method is used since it maintains the same sensitivity for all current settings.

Provision is made for controlling the electrodes manually, either individually or in a group. This facilitates handling of the electrodes during charging, pouring, and changing electrodes.

Figure 4 shows a front-view arrangement of the regulating equipment in a single metal-enclosed unit. A hinged panel door is used on the front of the regulator cabinet to permit access to various tubes and their connections. A bolted rear plate is provided in two sections so that any one or all of the regulator panels can be readily removed. The control panel is arranged for setting into the wall of the furnace vault in the usual manner.

The equipment is made so far as possible in one unit so that the various parts can be protected from dirt and physical damage. This also permits factory assembly and complete test, up to the operation of the electrode motors. A minimum of external connections and erection time is thus required.

Figure 5 is an oscillogram showing typical operation of the furnace during the melt-down period. This demonstrates the high speed of the electrode. Unsustained sudden variations in current cause no extra action of the equipment, but sustained changes cause immediate response. It will be noted that the electrode is withdrawn at relatively high speed when a cave-in takes place.

The start of the picture shows the electrode in comparatively stable operation, then suddenly the arc current is stopped indicating the arc has been broken. Instantly voltage is applied to the electrode motor to lower the electrode and re-establish the arc. Contact is made and the arc is drawn out until the arc voltage and current are returned to the desired quantities. The film shows this operation to have taken place in approximately 19 cycles. It should be appreciated that during the melt-down period, electrical conditions change rapidly, necessitating more or less continual electrode movement.

Figure 6 is an oscillogram showing performance during a portion of the refining period. Here relatively small changes in current take place and consequently only small movement of electrodes is required. Because of the inertia of the motors and electrodes, sudden-unsustained variations do not cause movement of the electrodes.

Figure 7 shows a high-speed oscillogram taken during the refining period. This

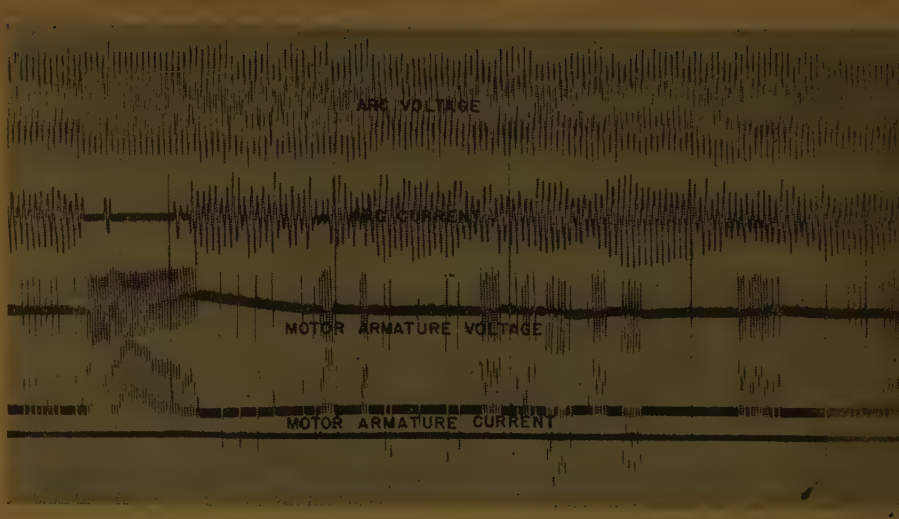


Figure 5. Oscillogram showing electrical performance of regulator during melt-down period

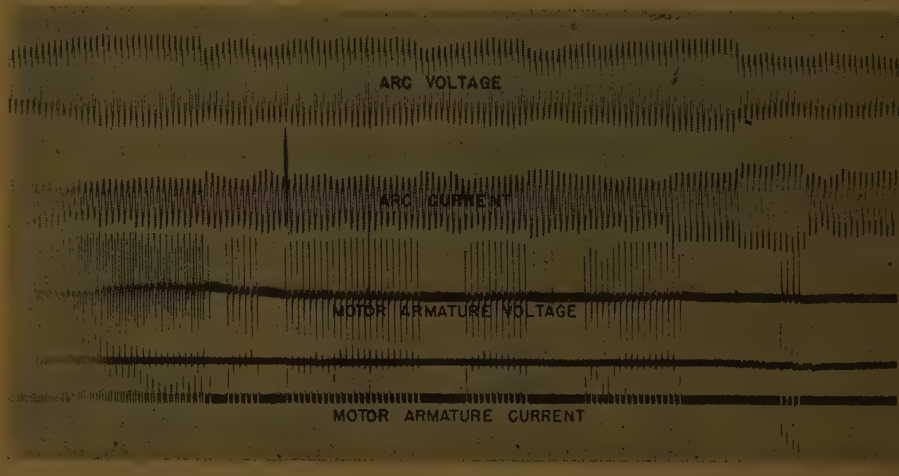


Figure 6. Oscillogram showing elecrical performance of regulator during refining period

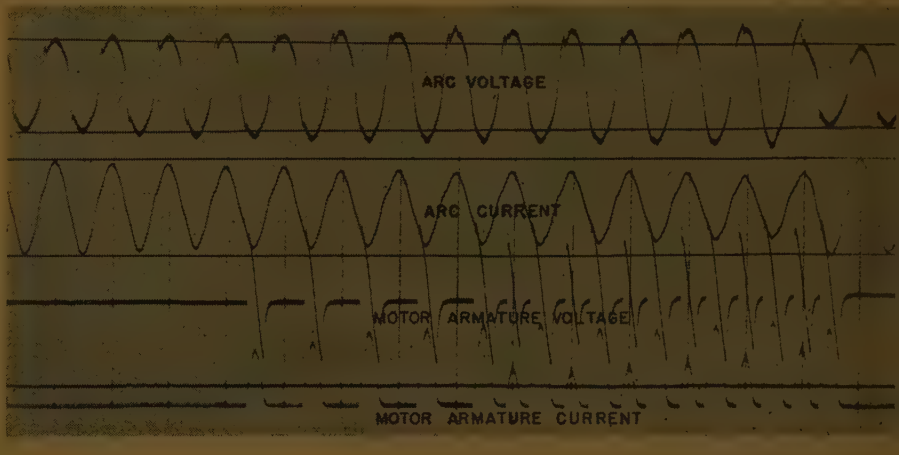


Figure 7. High-speed oscillogram showing electrical performance of regulator during refining period

film shows quite accurately the high speed of response. The picture shows the start of an unbalance and in less than two cycles voltage has been impressed across the motor armature. In 11 cycles the electrode has been returned to its desired position.

Operating experience has shown that the quick response of this regulator is particularly advantageous during melt-down periods when cave-ins frequently occur. The high-speed withdrawal of electrodes prevents breaker tripping due to sustained overload.

The regulator described in this paper was designed for a furnace which requires relatively small electrode motors by reason of using counterbalanced electrodes

Formulas for the Four-Terminal Network Parameters of Uniform Ladder Networks

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Synopsis: In the treatment of the over-all behavior of a smooth transmission line, or uniform ladder network, one frequently wishes to deal with the commonly termed *A-B-C-D* parameters rather than the propagation function, to which the former are related by hyperbolic functions, and the characteristic impedances. However, the latter are the functions which are simply related to the network components. The *A-B-C-D* parameters may be given as power-series expansions of the variable α, where $\alpha^2 = Z_T Y_T$ and Z_T and Y_T are the respective total series impedance and shunt admittance. For the smooth line these are infinite series; but they are terminating series for the ladder network, the number of terms and the coefficients of the terms being functions of the number (M) of elementary π or T sections of which the network is composed. Formulas for these coefficients are developed and they are given in several alternate forms. Also included are recurrence formulas and some relations among the coefficients of the different parameters. One form for the coefficients is particularly interesting because it shows that each coefficient for the ladder network is the corresponding smooth-line coefficient multiplied by a polynomial consisting of unity plus a polynomial in non-zero powers of $1/M^2$. This puts in evidence the manner in which the two coefficients approach each other as M increases; and also leads to easily computable upper and lower bounds of the ladder-network coefficients, which may provide useful estimates of their magnitudes.

The Parameters of Four-Terminal Networks

THE general theory of four-terminal networks is well known and may appear in any one of several forms, depending on the combinations of external currents and voltages which are to be related. In the present discussion we are interested in specifying the sending-end voltage and current, each in terms of linear combinations of both receiver voltage and current. In the general case of Figure 1, which may be a nonsymmetrical network, the required relations may be written

$$\left.\begin{aligned} E_s &= AE_r + B'I_r \\ I_s &= C'E_r + DI_r \end{aligned}\right\} \quad (1)$$

where the notation is that of the figure and A, B', C', and D are constants of proportionality. (The primes on B and C are used here merely to allow a uniform notation in the final results.) The theory of smooth lines and recurrent ladder networks is usually given in terms of the surge impedances (two in the case of a nonsymmetrical network) and the propagation function. However, this is an artificial point of view when one is interested in the over-all behavior of the circuit, considered as a four-terminal network, the parameters of equations 1 being more suitable. Occasion will arise in the subsequent discussion to compare the formulas for the ladder network with those for the smooth line. Therefore, we begin by putting the smooth-line parameters into forms suitable to our purpose. The following relations are well known:

$$\left.\begin{aligned} E_s &= (\cosh\alpha)E_r + \sqrt{Z_T/Y_T}(\sinh\alpha)I_r \\ I_s &= \sqrt{Y_T/Z_T}(\sinh\alpha)E_r + (\cosh\alpha)I_r \end{aligned}\right\} \quad (2)$$

where Z_T and Y_T are respectively the total series impedance and shunt admittance of the line, and $\alpha^2 = Z_T Y_T$. The parameters of relations 2 may be put into our final form by expanding cosh α and sinh α in McLaurin's series, giving

$$\left.\begin{aligned} A(\alpha) &= \cosh\alpha \\ &= 1 + \frac{\alpha^2}{2!} + \frac{\alpha^4}{4!} + \frac{\alpha^6}{6!} + \ldots . \quad (a) \\ B'(\alpha) &= Z_T \frac{\sinh\alpha}{\alpha} \\ &= Z_T\left(1 + \frac{\alpha^2}{3!} + \frac{\alpha^4}{5!} + \frac{\alpha^6}{7!} + \ldots .\right) \quad (b) \\ C'(\alpha) &= Y_T \frac{\sinh\alpha}{\alpha} \\ &= (Y_T/Z_T)B'(\alpha) \quad (c) \\ D(\alpha) &= A(\alpha) \quad (d) \end{aligned}\right\} \quad (3)$$

It will be useful to introduce the notation

$$\left.\begin{aligned} B(\alpha) &= B'(\alpha)/Z_T \\ C(\alpha) &= C'(\alpha)/Y_T \end{aligned}\right\} = 1 + \frac{\alpha^2}{3!} + \frac{\alpha^4}{5!} + \frac{\alpha^6}{7!} + \ldots . \quad (4)$$

for future use.

Turning to a consideration of the ladder structure let us consider Figure 2 which shows, in the general case, the two possible forms of such a network. It is shown in Appendix I that for either of these circuits the parameters will be of the forms

$$\left.\begin{aligned} A(\alpha) &= a_0 + a_1\alpha^2 + a_2\alpha^4 + \ldots a_{S_1}\alpha^{2S_1} \\ B(\alpha) &= b_0 + b_1\alpha^2 + b_2\alpha^4 + \ldots b_{S_2}\alpha^{2S_2} \\ C(\alpha) &= c_0 + c_1\alpha^2 + c_2\alpha^4 + \ldots c_{S_3}\alpha^{2S_3} \\ D(\alpha) &= d_0 + d_1\alpha^2 + d_2\alpha^4 + \ldots d_S\,\alpha^{2S_4} \end{aligned}\right\} \quad (5)$$

The a, b, c, and d coefficients are functions of the k factors and it is further shown in Appendix I that, when the two circuits of Figure 2 are related as indicated in the figure; then

$$\left.\begin{aligned} A_a &= D_b \\ B_a &= C_b \\ C_a &= B_b \\ D_a &= A_b \end{aligned}\right\} \quad (6)$$

where the subscripts refer to the intended circuit of the figure. In view of this it is necessary to apply the future treatment to only the series-terminated network because the solution for the corresponding shunt-terminated circuit will be tacitly included. We will be dealing with a uniform network and therefore for our purpose $A = D$.

The Uniform Ladder Network

This discussion will apply to a ladder network consisting of M T sections with midseries termination. Such a circuit may be represented by Figure 2a in which $k_1 = k_N = 1/2M$ and all other k's are equal to $1/M$. In similarity with equation 2, when the network is specified by its characteristic impedance (Z_0) and propagation function (γ), we have the well-known relations

$$\left.\begin{aligned} E_s &= (\cosh M\gamma)E_r + Z_0(\sinh M\gamma)I_r \\ I_s &= (1/Z_0)(\sinh M\gamma)E_r + (\cosh M\gamma)I_r \end{aligned}\right\} \quad (7)$$

Thus, for this circuit

$$\left.\begin{aligned} A_T(\alpha) &= D_T(\alpha) = \cosh M\gamma \quad (a) \\ B_T(\alpha) &= (Z_0/Z_T)\sinh M\gamma \quad (b) \\ C_T(\alpha) &= (1/Z_0Y_T)\sinh M\gamma \quad (c) \end{aligned}\right\} \quad (8)$$

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and hydraulic lowering and raising means. It is evident that the output energy of such a regulator depends on the rating and size of the electrode motor. The size of the furnace having balanced electrodes which this type of regulator can handle depends therefore on the energy requirements of the electrode motors. For other types of furnaces certain changes in design would be required such as braking means for the motors. In the application described this has not been found necessary, thus resulting in simplification of the regulating equipment.

The T subscript on A, B, and C is used to indicate that the network is made up of T sections. Furthermore, Z_0 and γ are given by[1]

$$Z_0=\frac{\alpha\sqrt{4M^2+\alpha^2}}{2Y_TM} \quad \text{(a)}$$

$$e^{\gamma}=\frac{\sqrt{4M^2+\alpha^2}+\alpha}{\sqrt{4M^2+\alpha^2}-\alpha} \quad \text{(b)} \qquad (9)$$

In view of relations 9, and with some reduction after substitution in equations 8, we obtain

$$A_T(\alpha)=\frac{(4M^2+\alpha^2)^M}{2(4M^2)^M}\times\left[\left(1+\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}+\left(1-\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}\right] \quad \text{(a)}$$

$$B_T(\alpha)=\frac{(4M^2+\alpha^2)^{M+1/2}}{\alpha(4M)(4M^2)^M}\times\left[\left(1+\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}-\left(1-\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}\right] \quad \text{(b)} \qquad (10)$$

$$C_T(\alpha)=\frac{M(4M^2+\alpha^2)^{M-1/2}}{\alpha(4M^2)^M}\times\left[\left(1+\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}-\left(1-\frac{\alpha}{\sqrt{4M^2+\alpha^2}}\right)^{2M}\right] \quad \text{(c)}$$

The binomial theorem may be applied twice to finally arrive at a power-series representation of equations 10. The details of the conversion are given in Appendix II, and the results are

$$A_T(\alpha)=\sum_{\lambda=0}^{M}a_\lambda\alpha^{2\lambda} \quad \text{(a)}$$

$$B_T(\alpha)=\sum_{\lambda=0}^{M}b_\lambda\alpha^{2\lambda} \quad \text{(b)} \qquad (11)$$

$$C_T(\alpha)=\sum_{\lambda=0}^{M-1}c_\lambda\alpha^{2\lambda} \quad \text{(c)}$$

where

$$a_\lambda=\sum_{n=0}^{\lambda}\frac{(2M)!(M-n)!}{(2M)^{2\lambda}(2M-2n)!(2n)!\times(M-\lambda)!(\lambda-n)!} \quad \text{(a)}$$

$$b_\lambda=\sum_{n=0}^{\lambda}\frac{(2M-1)!(M-n)!}{(2M)^{2\lambda}(2M-2n-1)!\times(2n+1)!(M-\lambda)!\times(\lambda-n)!} \quad \text{(b)} \qquad (12)$$

$$c_\lambda=\sum_{n=0}^{\lambda}\frac{(2M-1)!(M-n-1)!}{(2M)^{2\lambda}(2M-2n-1)!\times(2n+1)!(M-\lambda-1)!\times(\lambda-n)!} \quad \text{(c)}$$

The preceding expressions are cumbersome but may be reduced to the following simple forms by the method indicated in Appendix III. The forms are:

$$a_\lambda=\frac{K_a(M,\lambda)}{(2\lambda)!} \quad \text{(a)}$$

$$\chi=\frac{K_b(M,\lambda)}{(2\lambda+1)!} \quad \text{(b)} \qquad (13)$$

$$c_\lambda=\frac{K_c(M,\lambda)}{(2\lambda+1)!} \quad \text{(c)}$$

where

$$K_a(M,\lambda)=\frac{(M+\lambda-1)!}{M^{2\lambda-1}(M-\lambda)!} \quad \text{(a)}$$

$$K_b(M,\lambda)=\frac{(2M^2+\lambda)(M+\lambda-1)!}{2M^{2\lambda+1}(M-\lambda)!} \quad \text{(b)} \qquad (14)$$

$$K_c(M,\lambda)=\frac{(M+\lambda)!}{M^{2\lambda+1}(M-\lambda-1)!} \quad \text{(c)}$$

Equations 13 have been written with the factorials in evidence to bring out the similarity with the corresponding coefficients for a smooth line as given by equations 3a and 4. That is, the coefficients for a uniform ladder network are the corresponding coefficients for a smooth line multiplied by correction factors which are given explicitly by equations 14.

Relations Among the Coefficients

The similarities among formulas 14 leads one to look for useful relations among them, and the following recurrence formulas are immediately apparent.

$$K_a(M,\lambda)=\left[1-\left(\frac{\lambda-1}{M}\right)^2\right]\times K_a(M,\lambda-1) \quad \text{(a)}$$

$$K_b(M,\lambda)=\left[1-\left(\frac{\lambda-1}{M}\right)^2\right]\times\frac{\left(1+\frac{\lambda}{2M^2}\right)}{\left(1+\frac{\lambda-1}{2M^2}\right)}K_b(M,\lambda-1) \quad \text{(b)} \qquad (15)$$

$$K_c(M,\lambda)=\left[1-\left(\frac{\lambda}{M}\right)^2\right]\times K_c(M,\lambda-1) \quad \text{(c)}$$

In view of the complexity of equation 15b a possibly more useful form for $K_b(M,\lambda)$ is desirable, and may be obtained by expressing it in terms of $K_a(M,\lambda)$. For completeness a similar form is given for $K_c(M,\lambda)$. The results are self-evident from equations 14, and are

$$K_b(M,\lambda)=\left(1+\frac{\lambda}{2M^2}\right)K_a(M,\lambda) \quad \text{(a)}$$

$$K_c(M,\lambda)=\left(1-\frac{\lambda^2}{M^2}\right)K_a(M,\lambda) \quad \text{(b)} \qquad (16)$$

Other relations can be found among the coefficients; but, because of their particular interest, only the following ones will be given. By writing equation 16b with λ replaced by $\lambda-1$ and substituting the resulting expression for $K_a(M,\lambda-1)$ in equation 15a one obtains the simple relation

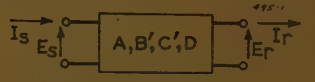


Figure 1

$$K_a(M,\lambda)=K_c(M,\lambda-1) \qquad (17)$$

Two less obvious relations may be obtained by starting from fundamental equations of the network. From equations 8 and 9 one may obtain

$$B=(1+\alpha^2/4M^2)C \qquad (18)$$

This is an identity and therefore when B and C are replaced by their series representations the corresponding coefficients of a given power of α, on the two sides of the equation, must be the same. Inspection shows that this leads to the result

$$b_\lambda=c_\lambda+\frac{1}{4M^2}c_{\lambda-1} \qquad (19)$$

which is valid for all values of λ if c_{-1} is defined as zero. One obtains the second of these two relations by starting from the well-known formula[2]

$$A^2-1=\alpha^2BC=\left(\alpha^2+\frac{\alpha^4}{4M^2}\right)C^2 \qquad (20)$$

Table I

λ	$K_a(M,\lambda)$ or $*K_c(M,\lambda-1)$	$K_b(M,\lambda)$
0	1	1
1	1	$1+\frac{1}{2M^2}$
2	$1-\frac{1}{M^2}$	$1-\frac{1}{M^4}$
3	$1-\frac{5}{M^2}+\frac{4}{M^4}$	$1-\frac{7}{2M^2}-\frac{7}{2M^4}+\frac{6}{M^6}$
4	$1-\frac{14}{M^2}+\frac{49}{M^4}-\frac{36}{M^6}$	$1-\frac{12}{M^2}+\frac{21}{M^4}+\frac{62}{M^6}-\frac{72}{M^8}$
5	$1-\frac{30}{M^2}+\frac{273}{M^4}-\frac{820}{M^6}+\frac{576}{M^8}$	$1-\frac{55}{2M^2}+\frac{198}{M^4}-\frac{275}{2M^6}-\frac{1,474}{M^8}+\frac{1,440}{M^{10}}$

* This has no meaning for $\lambda=0$.

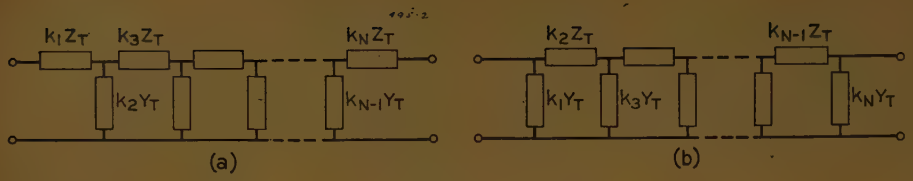


Figure 2

As in the previous case, one may substitute series for A and C and equate coefficients of like powers of α to give the formula

$$\sum_{q=0}^{\lambda} a_q a_{\lambda-q} = \sum_{q=0}^{\lambda-1} c_q c_{\lambda-1-q} + \frac{1}{4M^2} \sum_{q=0}^{\lambda-2} c_q c_{\lambda-2-q} \tag{21}$$

These last two relations (19 and 21) are superfluous so far as numerical computation is concerned but they are included for possible academic interest.

Additional Forms for the Coefficients

Neither of the forms so far given, in equations 12 or 13, for the coefficients gives a clear indication of how they vary with M, at a fixed value of λ. A better form, in this respect, may be obtained for $K_a(M, \lambda)$ and $K_c(M, \lambda)$ by the successive use of formulas 15a and 15c, and for $K_b(M, \lambda)$ by applying equation 16a to the new formula for $K_a(M, \lambda)$. The algebra of this transformation is self-evident, and the results are

$$\left.\begin{aligned} K_a(M,\lambda) &= \prod_{n=0}^{\lambda-1}\left(1-\frac{n^2}{M^2}\right) && \text{(a)}\\ K_b(M,\lambda) &= \left(1+\frac{\lambda}{2M^2}\right)\times \prod_{n=0}^{\lambda-1}\left(1-\frac{n^2}{M^2}\right) && \text{(b)}\\ K_c(M,\lambda) &= \prod_{n=0}^{\lambda}\left(1-\frac{n^2}{M^2}\right) && \text{(c)} \end{aligned}\right\} \tag{22}$$

From these it is seen that $K_a(M, \lambda)$ is a polynomial in even powers of $1/M$, the powers ranging from zero to $2\lambda-2$, inclusive; and that $K_b(M, \lambda)$ and $K_c(M, \lambda)$ are similar, but with even powers ranging from zero to 2λ, inclusive. Some of these polynomials have been computed and are given in Table I. It is interesting to note that equations 22 put in evidence the fact that a_λ and b_λ are zero if $\lambda > M$; and that c_λ is zero when $\lambda > M-1$. The form of equations 22 is particularly useful because it allows one to estimate the coefficients in terms of upper and lower bounds which may be obtained from the relation

$$\left[1-\left(\frac{\beta+1}{2M}\right)^2\right]^{\eta}\left(1-\frac{\beta^2+1}{M^2}\right)^{[\beta/2]} < \prod_{n=0}^{\beta}\left(1-\frac{n^2}{M^2}\right) < \left[1-\left(\frac{\beta+1}{2M}\right)^2\right]^{\beta} < 1 \qquad \beta < M \tag{23}$$

in which $\eta = 0$ or 1, depending on whether β is even or odd, and $[\beta/2]$ is the greatest integer in $\beta/2$. The derivation is given in Appendix IV. Taking the right side of this inequality allows one to write

$$\left.\begin{aligned} a_\lambda &< \frac{1}{(2\lambda)!} && \text{(a)}\\ b_\lambda &< \left(1+\frac{1}{2M}\right)\frac{1}{(2\lambda+1)!} && \text{(b)}\\ c_\lambda &< \frac{1}{(2\lambda+1)!} && \text{(c)} \end{aligned}\right\} \tag{24}$$

which is interesting because the reciprocal factorials are the corresponding smooth-line coefficients.

The a, b, and c coefficients can be given in still another form, in terms of the hypergeometric function $F(r, s, t, x)$. When $x = 1$ this function reduces to[4]

$$F(r,s,t,1) = \frac{\Gamma(t)\Gamma(t-r-s)}{\Gamma(t-s)\Gamma(t-r)}, \qquad \text{if real part } (t-r-s) > 0, \tag{25}$$

which may be used to represent any one of the expressions in equations 14 by suitable choices for r, s, and t. The following results are given for combinations of r, s, and t which reduce the multiplying factors to simple forms; but other choices would be possible. The correctness of the choices will become evident upon making the indicated substitutions. The results are

$$\left.\begin{aligned} a_\lambda &= \frac{1}{M^{2\lambda-1}}\,F(1-M+\lambda, 1-2\lambda, 2, 1) && \text{(a)}\\ b_\lambda &= \frac{2M^2+\lambda}{4M^{2\lambda+1}}\,F(2-M+\lambda, 1-2\lambda, 3, 1) && \text{(b)}\\ c_\lambda &= \frac{1}{M^{2\lambda+1}}\,F(1-M+\lambda, -1-2\lambda, 1, 1) && \text{(c)} \end{aligned}\right\} \tag{26}$$

Conclusions

In view of the mathematical form of the results the formulas themselves are their most concise representation and only a few interpretive remarks are considered necessary. In the use of these results it is to be remembered that, as given, they apply to the midseries termination; but that the midshunt-terminated case is represented if B and C are interchanged. Of the several forms given probably equations 14 and 22 are the most important. If all the coefficients are to be found, equations 22, or the recurrence formulas which are practically the equivalent, will be found to be the more convenient. However, if only an isolated coefficient is wanted it may be computed more conveniently by employing equations 14 in conjunction with tables of logarithms of the factorials, or Stirling's approximation, which is

$$\log_{10} n! = (n+1/2)\log_{10} n - n\log_{10} e + (1/2)\log_{10} 2\pi \tag{27}$$

Equations 22 have the greater utility from the interpretive standpoint. It has already been pointed out that this form puts in evidence the number of nonzero coefficients in the series for each parameter. The form also leads to relation 23 which will yield a quick estimate of a coefficient and may therefore be useful when M and λ are large (but λ not too near M) and the required accuracy does not necessitate the more extended computation of the exact formula. This inequality also leads to the interesting results given in relations 24; namely, that the coefficients or the A and C parameters are always less than the corresponding smooth-line coefficients, and that, for the B parameter, the ladder-network coefficients are less than $(1+1/2M)$ times the corresponding smooth-line coefficient.

The treatment has been entirely in terms of the A-B-C parameters. However the four-terminal network may also be specified in terms of the driving-point and transfer impedances (or admittances). The results are readily extended to give the latter parameters as rational functions of α^2 because the two types of parameters are related by simple rational forms. This ability to easily write the parameters as rational functions would find practical application where one is required to find the zeros or poles of these functions (or rational combinations of them), with respect to the variable $p = d/dt$, because explicit means are available for finding the zeros of a polynomial. It is well known that such zeros and poles are of importance in circuit analysis. In relation to this discussion it should be noted that α^2 is a rational function of p when the network consists of lumped elements.

Appendix I

For the network of Figure 2a we may write the $(N+1)/2$ mesh equations

$$\left.\begin{aligned} E_s Y_T &= I_s(k_1\alpha^2 + 1/k_2) - I_3/k_2\\ 0 &= -I_s/k_2 + I_3(1/k_2 + k_3\alpha^2 + 1/k_4) - I_5/k_4\\ &\cdots\cdots\cdots\cdots\cdots\cdots\\ 0 &= -I_{N-1}/k_{N-1} + I_r(1/k_{N-1} + k_N\alpha^2) + E_r Y_T \end{aligned}\right\} \tag{28}$$

where the numbered currents are the mesh currents which are numbered to correspond to the series impedances through which they flow. Figure 2b may be given a similar treatment, but on a nodal basis,[3] with the result which follows:

$$\left.\begin{aligned} I_s Z_T &= E_s(k_1\alpha^2+1/k_2)-E_3/k_2 \\ 0 &= -E_s/k_2+E_3(1/k_2+ \\ &\qquad k_3\alpha^2+1/k_4)-E_5/k_4 \\ &\cdots\cdots\cdots\cdots\cdots \\ 0 &= -E_{N-1}/k_{N-1}+E_T(1/k_{N-1}+ \\ &\qquad k_N\alpha^2)+I_T Z_T \end{aligned}\right\} \quad (29)$$

In these the voltage subscripts refer to the nodes at which the shunt elements having the same subscripts are terminated. It is seen that these two sets of equations are identical with respect to the k's and α, and are dually related with respect to voltage, current, impedance, and admittance. Equations 28 may be solved for E_s and I_s, in terms of E_T and I_T, and the solutions are

$$\left.\begin{aligned} E_s &= A(\alpha, k_1, k_2, \ldots)E_T+ \\ &\qquad Z_T B(\alpha, k_1, k_2, \ldots)I_T \\ I_s &= Y_T C(\alpha, k_1, k_2, \ldots)E_T+ \\ &\qquad D(\alpha, k_1, k_2, \ldots)I_T \end{aligned}\right\} \quad (30)$$

where A, B, C, and D are polynomials in α, the coefficients of which are functions of the k's. The above-mentioned relation between equations 28 and 29 shows that the solutions of equations 29 are

$$\left.\begin{aligned} I_s &= A(\alpha, k_1, k_2, \ldots)I_T+ \\ &\qquad Y_T B(\alpha, k_1, k_2, \ldots)E_T \\ E_s &= Z_T C(\alpha, k_1, k_2, \ldots)I_T+ \\ &\qquad D(\alpha, k_1, k_2, \ldots)E_T \end{aligned}\right\} \quad (31)$$

Relations 6 follow from a comparison of equations 30 and 31.

Appendix II

The method used to arrive at the formulas for A, B, and C for the uniform ladder network will be given by the use of A as an example. The same procedure was used for B and C. We start by expanding each of the binomials of equation 10a by the binomial theorem. The signs are such that odd powers of the argument cancel and we obtain

$$A=\frac{1}{(4M^2)^M}\sum_{r=0}^{M}\frac{(2M)!}{(2M-2r)!(2r)!}\times (\alpha^2)^r(4M^2+\alpha^2)^{M-r} \quad (32)$$

The binomial expansion may now be applied to the last term in the summation, giving

$$A=\sum_{r'=0}^{M}\frac{(2M)!}{(4M^2)^r(2M-2r)!(2r)!}(\alpha^2)^r\times \sum_{s=0}^{M-r}\frac{(M-r)!}{(M-r-s)!(s)!}\left(\frac{\alpha^2}{4M^2}\right)^s \quad (33)$$

which may be written as

$$A=\sum_{r=0}^{M}\frac{(2M)!}{(2M-2r)!(2r)!}\times \sum_{s=0}^{M-r}\frac{(M-r)!}{(M-r-s)!(s)!}\left(\frac{\alpha^2}{4M^2}\right)^{r+s} \quad (34)$$

This is conveniently transformed by changing the summation indices to r and u, where $u=r+s$, resulting in

$$A=\sum_{r=0}^{M}\frac{(2M)!(M-r)!}{(2M-2r)!(2r)!}\times \sum_{u=r}^{M}\frac{1}{(M-u)!(u-r)!}\left(\frac{\alpha}{2M}\right)^{2u} \quad (35)$$

The above may be rearranged by factoring out the various powers of α. For example, let λ be a particular value of u, it being obvious that λ may be between 0 and M, inclusive. For this value, one coefficient of $\alpha^{2\lambda}$ will be obtained for each value of r in the first summation for which $\lambda \geqq r$, because the lower limit on the second summation is r. Thus, r may be replaced by another index (n) which runs from 0 to λ, inclusive, and the result is as given by the combination of equations 11a and 12a.

Appendix III

An outline of the complete proof for the reduction to forms 14 will be given for a_λ. The usual notation for a binomial coefficient will be used, namely

$$\binom{p}{q}=\frac{p!}{q!(p-q)!}$$

In this notation we are to prove

$$\sum_{n=0}^{\lambda}\binom{2M}{2n}\binom{M-n}{\lambda-n}=\frac{2^{2\lambda}M(M+\lambda-1)!}{(2\lambda)!(M-\lambda)!} \quad (36)$$

The method is to write the function

$$\phi(x)=(1-x^2)^{-M+\lambda-1}(1+x)^{2M}=\sum_{q=0}^{\infty}G_q x^q \quad (37)$$

and obtain two expressions for $G_{2\lambda}$, one which will be the left side of equation 36 and the other which will be the right side. The function is analytic and the series converges when $|x|<1$. With some manipulation for the first of the following forms, and directly for the second, one obtains

$$\left.\begin{aligned} (1-x^2)^{-M+\lambda-1} &= \sum_{r=0}^{\infty}\binom{M-\lambda+r}{r}x^{2r} \quad \text{(a)} \\ (1+x)^{2M} &= \sum_{s=0}^{2M}\binom{2M}{s}x^s \quad \text{(b)} \end{aligned}\right\} \quad (38)$$

$G_{2\lambda}$ is obtained from equations 38 by summing the product of the general terms of these two series over all values of r and s such that $r\geqq 0$, $0\leqq s\leqq 2M$, and $2r+s=2\lambda$. Thus, s must be even, say $2n$, and therefore $r=\lambda-n$. With the substitution of these in the general terms of equations 38, and taking their products, one arrives at the left side of equation 36.

The second part of the proof is obtained from the Cauchy integral formula for $\phi^{(2\lambda)}(0)/(2\lambda)!$ (the McLaurin-series coefficient of $x^{2\lambda}$). This formula is

$$G_{2\lambda}=\frac{1}{2\pi j}\int_C\frac{\phi(x)}{x^{2\lambda+1}}dx \quad (39)$$

where C is a circle of radius less than unity, with its center at the origin, and over which the integration is to be in a counterclockwise direction. The integral is easily evaluated by employing the change of variable $x=1/u$ which results in

$$G_{2\lambda}=\frac{1}{2\pi j}\int_{C'}u(u+1)^{M+\lambda-1}(u-1)^{-M+\lambda-1}du \quad (40)$$

taken counterclockwise about the origin on a circle of radius greater than unity. The integrand has a pole at $u=1$, which is inside the path of integration, and is analytic everywhere else inside the path of integration. Hence, $G_{2\lambda}$ is the residue of the integrand at $u=1$. The Laurent series for $\psi(u)$, the integrand, may be shown to be

$$\psi(u)=2^{M+\lambda-1}\sum_{r=0}^{M+\lambda-1}\frac{1}{2^r}\binom{M+\lambda-1}{r}\times (u-1)^{-M+\lambda-1+r}+2^{M+\lambda-1}\times \sum_{r=0}^{M+\lambda-1}\frac{1}{2^r}\binom{M+\lambda-1}{r}(u-1)^{-M+\lambda+r} \quad (41)$$

The residue is the coefficient of $(u-1)^{-1}$ and is obtained by putting $r=M-\lambda$ in the first sum, and $r=M-\lambda-1$ in the second. The resulting form is readily reducible to the right side of equation 36.

Similar proofs may be given for equations 14b and 14c. In both cases one finds two formulas for the coefficient of $x^{2\lambda+1}$ in the expansion of $\phi(x)$; but in the case of equation 14c the function $(1-x^2)^{-M+\lambda}$ is substituted for $(1-x^2)^{-M+\lambda-1}$ in $\phi(x)$.

Appendix IV

To arrive at relation 23 observe that the continued product may be written

$$\prod_{n=0}^{\beta}\left(1-\frac{n^2}{M^2}\right)=\left[1-\left(\frac{\beta+1}{2M}\right)^2\right]^{\eta}\times \prod_{n=0}^{\left[\frac{\beta}{2}-1\right]}\left[1-\left(\frac{\frac{\beta+1}{2}-\Delta}{M}\right)^2\right]\times \left[1-\left(\frac{\frac{\beta+1}{2}+\Delta}{M}\right)^2\right] \quad (42)$$

where

if β is even, $\eta=0$ and $\Delta=n+1/2$
if β is odd, $\eta=1$ and $\Delta=n+1$

and the brackets on $\beta/2-1$ indicate the greatest integer in $\beta/2-1$. The continued

Improved Selective Tripping of Low-Voltage Air Circuit Breakers

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SINCE the first devices for interrupting short-circuit currents in electric systems were invented, the problem of having them operate to clear faults without removing energy from normal circuits has existed. Many devices, both separate and as parts of the interrupters, have been developed and good results obtained. Systems having potentials below 600 volts alternating current normally use air interrupting devices designed particularly for this service.

The normal utilization of a-c power in industrial plants or generating station auxiliary circuits where the average individual load is less than about 75 horsepower is at 600 volts or less. Occasional larger loads and groups of smaller loads, switched as units, require a considerable range in both current-carrying capacity and interrupting ability of circuit breakers. The standard devices available have current ratings up to 6,000 a-c amperes and 100,000 amperes interrupting rating. Larger units have also been built to a limited extent.

When a large number of small loads are supplied, the total power required determines the size of the generating or transforming apparatus and thus indirectly the values of fault currents. Even though individual loads may be small, high interrupting requirements may dictate the use of relatively large circuit breakers. They may become so large that their cost is out of proportion to the motors or other devices to which they supply power. The economics of low-voltage system design, therefore, require circuit arrangements which will permit the use of the smallest possible switching devices. At the same time, selective isolation of faults is highly desirable. However, this is frequently partially sacrificed in special circuits arranged to permit the use of circuit breakers in duty above their interrupting ratings (cascading). New developments in built-in tripping devices will provide full selectivity without the use of separate relays in normal breaker applications and increase the ranges of reliable selective tripping in special circuit arrangements in which breakers are applied whose interrupting capacities are below the value of maximum fault currents.

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Selective Tripping With Breakers Having Full Interrupting Ability

Circuit breakers supplying power to loads should operate to clear only faulted circuits and not interrupt energy to other parts of the system. This requirement can sometimes be modified, but in circuits for essential central-station auxiliaries and certain industrial processes, maximum continuity of service is essential. This means, referring to Figure 1, that breakers A, B, and C must have interrupting capacity fully equal to the fault currents which may occur, that they must have time discrimination on tripping, and that they must have short-time ratings adequate for the time and current to which they will be subjected. These distinctions, as compared with the special "cascade" arrangement discussed later, where only limited selectivity is obtainable, should be kept in mind throughout the following discussion.

The problems arising from selective tripping are all recognized in high-voltage breaker applications and standard devices meet them. These are usually protective relays which open the breakers by energizing their shunt trip coils. The situation on low-voltage air breakers is somewhat different. The designs of all manufacturers have almost uniformly included instantaneous trip attachments operating at 15 times normal current on ratings below 2,000 amperes and 12 times normal above this value. No short-time current ratings were assigned. This has meant that the amount of conducting material in the contacts is relatively less than has been considered necessary in high-voltage designs. Moves are now under way to assign short-time ratings to all low-voltage breakers. However, the maximum values possible for existing breakers, for reasons just explained, probably will be somewhat lower compared to the interrupting current rating, than has been the case with high-voltage power breakers. This must be taken into consideration in applying breakers where intentional time delay in tripping is introduced to obtain selectivity and affects cost as compared to the cascade arrangement described in detail later.

When breakers B and C are in separate structures and connected by cables, the problem of protecting these cables enters the picture. A relatively long tripping time is desirable, operating at the cable current capacity, a shorter tripping time within the short-time current rating of the breaker is needed for selectivity and instantaneous tripping above this current. Present commercially available self-contained tripping devices offer only the time characteristics of oil dashpots or thermal devices and these, at best, are questionable. Protective relays offer the best available means for obtaining the tripping time values necessary for these various current values.

Many engineers are satisfied to have short-circuit protection only on power supply cables on the basis that they are protected from overloads by the feeder breakers. This is sound practice on a properly designed distribution system,

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product on the right side of equation 42 may be written

$$\prod_{n=0}^{\left[\frac{\beta}{2}-1\right]}\left[1-\frac{\beta^2+2\beta+1+4\Delta^2}{2M^2}+\frac{(\beta^2+2\beta+1-4\Delta^2)^2}{16M^4}\right] \quad (43)$$

A lower bound of this quantity may be obtained by neglecting the third term and assigning to Δ its largest value, namely $(\beta-1)/2$ in each case. Similarly, an upper bound will result if $4\Delta^2$ is neglected in all terms. These substitutions, by eliminating the variable n, permit the substitution of an exponent for the product, and readily yield the results given in relation 23.

References

1. COMMUNICATION NETWORKS (book), E. A. Guillemin. John Wiley and Sons. Volume II, page 178.

2. Reference 1, page 139.

3. ELECTRIC CIRCUITS (book), Massachusetts Institute of Technology staff. John Wiley and Sons. Page 463.

4. MODERN ANALYSIS (book), Whittaker and Watson. Cambridge University Press. Fourth edition, page 281.

but there are arrangements where the best planning cannot eliminate the hazard of overloading. In these cases, discriminatory tripping must be provided or continuity of service partially sacrificed.

The application of relays for obtaining selective tripping with suitable breakers on the basis of time discrimination is well understood and only self-contained tripping devices will be considered here.

Cascaded Breakers

If we refer again to Figure 1 which illustrates typical circuits, it is evident that if breaker *B* and one of the *C* breakers both open at substantially the same time to clear a fault, the interrupting duty on *C* will be materially reduced. If tripping devices are supplied for *B* so that this will always take place, then a smaller breaker can under certain conditions be used in position *C* than would have been possible in the circuits described previously. A similar relation exists between breakers *A* and *B*. This often results in a material saving in the cost of circuit breakers. The advantages, limitations and conditions determining the selection of interrupting devices in cascade circuits of this kind will be considered.

Manufacturers have recognized the advantages of this arrangement and have approved special cascaded circuits as covered by the following definition in the National Electrical Manufacturers Association Standards.

"Cascading is the application of air circuit breakers in which only the breakers nearest the source of power shall have interrupting ratings equal to, or in excess of, the obtainable fault current while breakers in succeeding steps further from the source may have successively lower interrupting ratings."

The relative allowable reduction in interrupting rating of the breakers in successive steps is limited as follows. Group breakers *B* may have 50 per cent of the interrupting requirements of main breaker *A* and branch breakers *C* in turn may have one-third the rating of the main breaker which must be able to open on a standard duty cycle the maximum fault current which the power supply can deliver. Cascading is limited to three steps of interrupting rating. In many applications, this represents a great saving in breaker cost as compared to an installation in which all devices have full interrupting ability. However, it must be realized that this saving is not obtained without some risk and sacrifice in over-all performance.

The main and group breakers must be free to trip instantaneously to afford maximum protection to the smaller branch breakers which are allowed to be of reduced interrupting ratings. This requirement is the all-important and definite difference between cascaded and similar circuits previously described in which all breakers have the same full interrupting ability and selectivity of tripping depends on time discrimination.

The instantaneous tripping devices of the main and group breakers must be set at about 80 per cent of the interrupting rating of those in the next lower step in the cascade. This margin is necessary to allow for some manufacturing variances and to insure that the speed of opening will not be too slow at currents just over the interrupting ability of the breaker in the next lower step. These trip calibrations mean that faults on load circuits may open the group or even the main breaker depending on whether the current reaches the trip settings of these devices. This limitation is fundamental in the cascade arrangement and must be accepted as one of the offsetting factors against the savings in equipment.

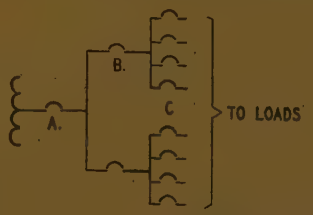


Figure 1. A common arrangement of low-voltage breakers

At present, low-voltage air breakers have no published short-time current ratings. These are being determined by test for existing designs and will soon be announced. When this takes place, the tripping devices on main and group breakers may be set to allow delayed tripping up to their short-time rating with instantaneous opening at currents above this figure. It is apparent that better continuity of service and selective isolation of faults will be obtained under these conditions. However, there will still be many conditions in cascaded circuits having breakers with successively reduced interrupting and short-time ratings where opening must be instantaneous.

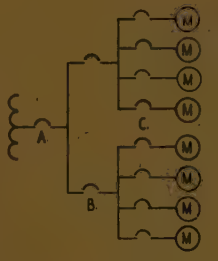


Figure 2. A typical cascaded power supply to motors listing the breaker characteristics for co-ordinated mechanically timed tripping

	Breaker *A*	Breaker *B*	Breaker *C*
Current rating, amperes	1,600	600	50
Interrupting rating, amperes	50,000	25,000	15,000
Half-second short-time rating, amperes	25,000	18,000	10,000
Instantaneous trip setting, amperes	20,000	14,400	500
Time-delayed trip settings, seconds	0.4	0.2	*

* Thermal time delay set to allow motor starting and provide motor overload protection.

At this point, it should be noted that the short-time ratings which can be assigned to low-voltage breakers designed for instantaneous opening on 12 or 15 times their continuous rating will have to be materially lower than their interrupting rating. This does not parallel the situation on higher-voltage circuit breakers where short-time ratings equal or occasionally exceed their interrupting ratings. It is also very doubtful whether the cost of future low-voltage designs should be increased to provide higher short-time ratings. Most applications can be satisfied with present constructions and an increased number of standard ratings is not economically desirable. The requirements of a few applications should not burden all the others by dictating high short-time ratings in all standard designs.

A reference to Figure 1 will make it evident that:

1. Breaker *B* must open instantaneously when its short-time rating is exceeded.
2. In the cascade arrangement, with successively smaller steps of interrupting capacity and short-time ratings, close-in faults on feeders may exceed the short-time ratings of the group breaker.
3. Consequently selective tripping is not always possible.

A further condition applying to cascaded breakers is the restricted duty cycle which is imposed. A breaker operated within its rating is allowed maximum-current "O" and "CO" operations separated by a two-minute interval and should be able to carry rated load after such openings. Breakers operating above their rating in a cascade circuit are restricted by requiring inspection and possible maintenance after each opening on fault current. This means that it is not safe to reclose cascaded breakers, even once, without inspection if two in series have cleared a short circuit. The smaller breaker might be damaged so that, even though its contacts completed the circuit, they would not safely carry

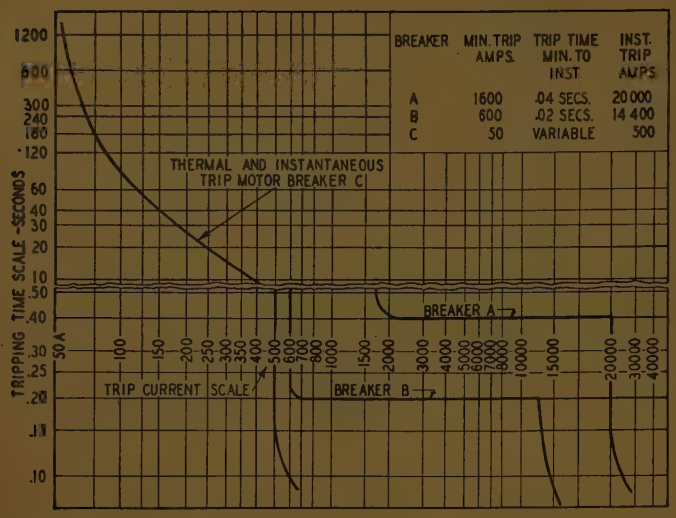


Figure 3. Time–current tripping coordination of cascaded breakers with integral mechanically delayed trips

The horizontal lines represent time bands of appreciable width and must be separated approximately as shown

load current. Inspection is necessary after each fault opening when breakers are applied with inadequate interrupting ability as in cascaded circuits.

To sum up the case for cascaded breaker circuits; on the affirmative side, breaker expense is often materially reduced; on the negative side, continuity of power is partially sacrificed, increased breaker maintenance may be expected, and a more limited duty cycle is specified for most of the breakers. Cascade arrangements applied with a full understanding of these conditions should prove satisfactory.

Cascaded Breakers With Selective Tripping

New definite-time tripping devices are being developed which will give breakers so equipped equal or better selectivity than is obtainable with relays, and will also improve the operation of cascade arrangements. Tests with as many as three breakers in series have shown accurate selective tripping with a maximum time setting of less than one-half second on the largest breakers. The diagram, Figure 2, has been prepared to illustrate such conditions. This arrangement of breakers will provide instantaneous tripping in the ranges required to permit a cascaded circuit with the allowed reduction in interrupting rating in each breaker step farther from the supply. The consequent economics are realized and the new tripping devices will function to give all the selectivity which the chosen breakers will permit.

Let us now review the operation of this cascaded circuit under a number of fault and load conditions. Assume motor *M* has a full-load current of 50 amperes. Breaker *C* will provide the usual thermal protection. It will open instantaneously on a motor fault exceeding ten times normal or 500 amperes. If the fault is over 15,000 amperes, that is, above the interrupting ability of *C*, breaker *B* will open instantaneously to back up *C* and so on up through the current range and breaker series. If the fault is below 15,000 amperes but for some reason breaker *C* fails to clear, breaker *B* will still open in 0.2 second. If it, in turn, should not interrupt, breaker *A* will open in 0.4 second. Faults which occur anywhere in the circuit will:

1. Open the nearest breaker.
2. Open the next larger breaker instantaneously if its assistance is needed for interruption.
3. Open the latter breaker in a slightly longer time if the current is within the interrupting ability of the first breaker, but it has failed to clear.
4. Open succeeding breakers in a similar manner if needed.

Reference to Figure 3 will show the current and time characteristics which give these results.

Considering the time intervals during which breakers will be subjected to high fault current in this arrangement, breaker *C* would be a thermally tripped breaker with a self-protecting instantaneous trip set at 500 amperes, breaker *B* would be set to open in 0.2 second on currents up to 14,400 amperes, 80 per cent of its short-time rating, and breaker *A* would be set to trip in 0.4 second, up to 20,000 amperes, 80 per cent of its half-second rating. This allows each breaker, provided its short-time rating is not exceeded, to hold in a sufficient time for the breaker next lower in the cascade to clear. This ensures that all breakers adequate to carry fault current arising from a short circuit remain closed and all others trip.

It is evident that the above degree of selective tripping gives discriminatory opening up to the capacity of the breakers in the cascaded circuit. No better selectivity is possible with breakers applied in accordance with standard cascading practice. However, there may be fault current values where group breakers occasionally open and de-energize unfaulted circuits. The savings due to the use of smaller breakers will usually justify this. However, a similar circuit made up entirely of breakers with definite-time tripping attachments, full interrupting ability, and adequate short-time ratings will give complete selectivity without the use of relays and shunt trip coils.

It has been shown, first, that full selectivity could be obtained with breakers having full interrupting ability and adequate short-time ratings in all steps and tripped by relays giving definite time discrimination in faults. Second, the possibilities and economies of cascaded breakers and the limitations of this system were considered. Third, it was explained that new tripping devices are being developed for standard breakers combining instantaneous and time-delay tripping features so that the economics of cascaded breakers can be realized and short-time ratings and interrupting ability used to the best advantage to give maximum permissible selectivity. It was also pointed out that adequate breakers with definite-time tripping devices in all positions can give complete selectivity, the equivalent of which has previously been possible only with relays, shunt trips, and storage batteries. Thus we advance another step in the economic use of materials coupled with a better accomplishment of purpose in improved continuity of service.

The Degree of Short-Circuit Protection Afforded Small Low-Voltage A-C and D-C Starters by Means of Fuses and Circuit Breakers

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AIEE technical paper 42-79, entitled "Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less,"[1] was presented in April 1942. The purpose of the article as stated was "to show what the characteristics of the protective devices should be in order that the power will be disconnected from the main feeders under all conditions of excess current before excess temperatures are attained." After the overload protecting requirements were discussed, it was pointed out that "to protect the conductors in the branch circuit adequately, the control devices, and the motor winding, we must use a device that is both fast in opening the circuit and also has the necessary interrupting ability." In discussing the speed and the interrupting abilities of circuit breakers and fuses which were used to protect the motors and the controls against short circuits, the following statement was made:

"The second half of the question dealing with the amount of available energy which these fuses can interrupt was determined by providing a 250-volt d-c source which was capable of delivering 96,000 amperes and also a three-phase source which was capable of delivering an rms symmetrical current of 84,000 amperes at 440 volts and 114,000 amperes at 600 volts. In the d-c circuit were placed two standard nonrenewable General Electric 250-volt fuses in series to simulate the usual two fuses that are in a circuit. Two 600-volt fuses were also tested in this circuit wherein the available currents were varied from 10,000 to 96,000 amperes in several steps, and the results were perfect. The buildup of the current in this circuit was much slower than in an a-c circuit so that the current-limiting effects were much more pronounced as shown in Figure 7.

"In the three-phase 440-volt a-c circuit one 250-volt fuse of the preceding type was connected in each supply line, and the available currents ranged from 18,000 to 84,000 symmetrical rms amperes. The resulting currents are shown in Figure 6, and the performance was perfect. When the supply power was 600 volts, then one 600-volt fuse was connected in each supply line, and the available current ranged from 24,000 to 114,000 amperes. The results are also shown on Figure 6 which was perfect in operation. It can be said therefore that *with this arrangement of connections* (note that Figure 6 gives the impedance of each combination of the a-c circuits), these fuses are capable of interrupting a circuit, either a-c or d-c, which has an available short-circuit current of 100,000 amperes."

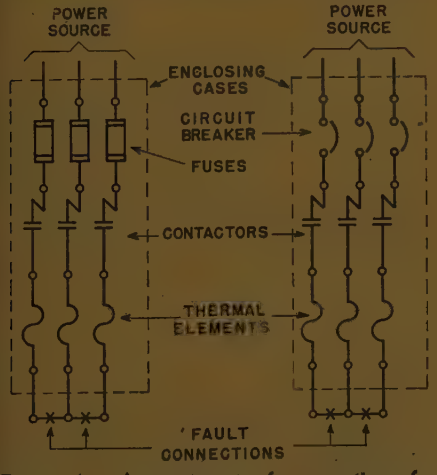


Figure 1. Arrangement of connections for fuses or circuit breakers when used for protecting a-c starters

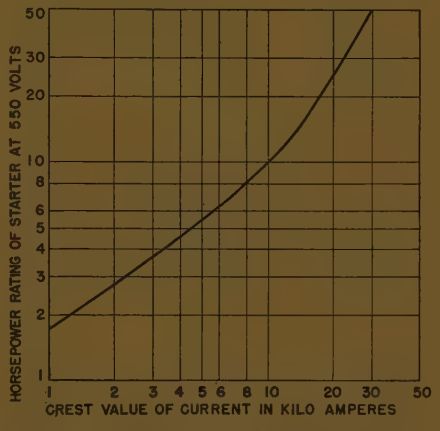


Figure 2. Approximate short-circuit currents (crest value) which will flow through a three-phase across-the-line starter when connected to a 550-volt three-phase supply that has an available current of 50,000 rms symmetrical amperes—short circuit on load side of starter

The preceding statement regarding the interrupting abilities of fuses has been interpreted by some differently from the original intent, and it is the purpose of this supplementary paper to point out the interpretation which should be made. Some further data will be submitted to show the relative protecting characteristics of these fuses and of some small-size air circuit breakers which are commonly used for protecting small-size starters.

As was stated in the purpose of the article, the investigation was to ascertain the degree to which the fuses and the circuit breakers would protect the starters from damage due to short circuits passing through the starters. Therefore, every test had a starter in series with either a set of appropriate-size fuses or a corresponding-size circuit breaker as illustrated in Figure 1 of this present paper. Therefore the amount of current which would flow was affected appreciably by the size of starter that was in the circuit. The original statement was not intended to imply that the fuses would give the same performance when the starters were omitted from the circuit. As previously noted, it stated that *with this arrangement of connections* the fuses were capable of interrupting a circuit having an available current of 100,000 amperes.

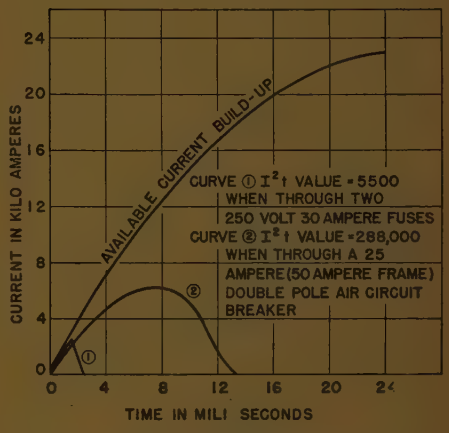


Figure 3. Current build-up following a short circuit on a 250-volt d-c supply and the I^2t values which were permitted to pass through 30-ampere fuses and through a 25-ampere circuit breaker

To emphasize further the importance of this difference, Figure 2 shows the approximate current which would flow if various sizes of three-phase starters, rated according to horsepower at 550-volt three-phase power supply, which had an available short-circuit current of 50,000 rms symmetrical amperes, were connected to the supply when a three-phase short circuit was placed on the load side of the starters. This shows that the value of the short-circuit current is determined more by the size of the starter, for the sizes under con-

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sideration, than by the size of the power supply. Therefore, the small-size fuses have to interrupt the smaller currents and the larger fuses the larger currents which is in accordance with their abilities. Therefore, our tests showed that when the several features that were enumerated in the original paper obtained, then the fuses were able to give the necessary protection as originally claimed.

These data also emphasize the importance of combining the short-circuit protecting means and the starter into one enclosure as illustrated in Figure 1 so as to minimize the probability of a short circuit occurring between the protecting means and the starter. Then, if the short circuit should require the assistance of the protecting means, the starter will be in the path of the short circuit, and the resultant current will be within the range of what the fuse can handle.

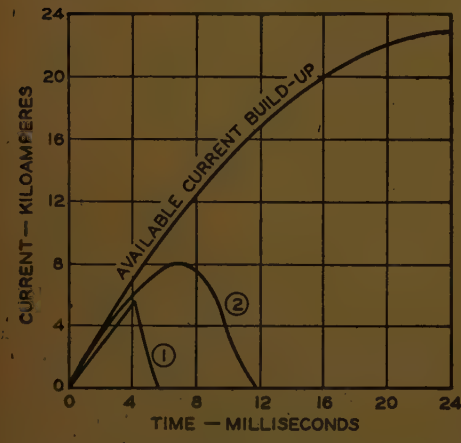


Figure 4. Current build-up following a short circuit on a 250-volt d-c supply and the I^2t values which were permitted to pass through 60-ampere fuses and through a 50-ampere circuit breaker

Curve 1 I^2t value = 62,000 when through two 250-volt 60-ampere fuses
Curve 2 I^2t value = 352,000 when through a 50-ampere double-pole circuit breaker

Also, in support of the superior protection claimed for the fuses in contrast to the protection which circuit breakers give to those small-size starters (size 3 and less), Figures 3, 4, and 5 are offered. The comparison shown is the I^2t values (the current squared times the duration in seconds) which two 250-volt fuses of the 30-, 60-, and 100-ampere sizes and double-pole circuit breakers (standard 50- and 100-ampere frame sizes of breakers) permitted to flow into a short circuit from a 250-volt d-c supply which had an available short-circuit current of 23,000 amperes. The I^2t values which these two devices permit to get through into a short circuit is a direct measure of the amount of heat that will be put into some thermally weak member that should be protected. The reason why a d-c supply was chosen on which to base this comparison was that in many cases it is essential that this short circuit be removed in less than one-half cycle, and if direct current is used then the initial rate of current build-up and the voltage at interruption always will be the same, and whatever difference there is in the I^2t value will be due to the difference in the interrupting characteristics of the two devices. In these three figures the available current from the power source is plotted and, curve 1 the build-up and cutoff as determined by the fuse, and curve 2 the buildup and cutoff as determined by the circuit breaker. It will be noted that two 30-ampere 250-volt fuses are compared to a double-pole 25-ampere (50-ampere frame) circuit breaker; two 60-ampere fuses are compared to a double-pole 50-ampere circuit breaker; and two 100-ampere fuses are compared

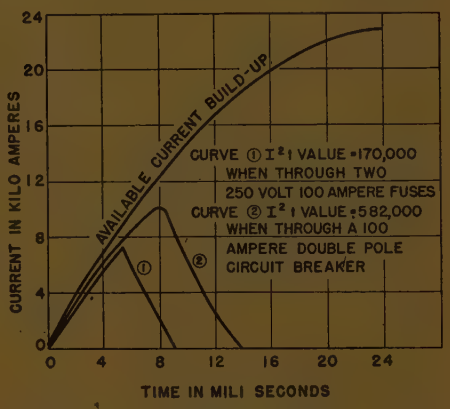


Figure 5. Current build-up following a short circuit on a 250-volt d-c supply and the I^2t values which were permitted to pass through 100-ampere fuses and through 100-ampere circuit breakers

to a double-pole 100-ampere (100-ampere frame) circuit breaker. These data show that the respective I^2t values which these devices permitted to pass were 5,500 versus 288,000; 62,000 versus 352,000; 170,000 versus 582,000. This very definitely p oves that the fuse does a very much superior job in protecting the smaller device against excess energy.

Because of the aforementioned situation, it may be constructive to show the range of thermal capacity of the heaters of the overload relays which are very commonly the thermal limiting factor on most starters. The minimum curve in Figure 6 shows the I^2t values which the overload relay heaters may be able to withstand. As already shown, the 60-ampere fuse would be used in conjunction with an overload relay rated about 17 or 18 amperes, and Figure 6 shows that the minimum value may be about 70,000, and the test data showed that the fuse permitted 62,000 to get through but the circuit breaker permitted 352,000 to pass. The maximum curve shows that there are some forms of relays which have a permissible I^2t value of 700,000. But, since the trend is toward the faster tripping relay so as to protect the fast-heating motors, it is advisable that the fast short-circuit protective means be used so as better to protect either the fast- or the slow-heating type of overload relays.

Conclusions

When we consider that:

1. The fuse is capable of interrupting a short circuit on a large power supply of either alternating or direct current if the starter is in the path of the short circuit.

2. Under some conditions, the 30-ampere fuse will allow only *two per cent* and the 60- and 100-ampere fuses will allow only 25 to 30 per cent of what similar-size circuit breakers will permit to reach a short circuit under similar circumstances.

3. The general trend of motors is to require faster overload relays, and these faster relays generally will have less thermal capacity (more expensive and larger relays can be fast and still have large thermal capacity), but the user cannot be sure when purchasing control from the general market.

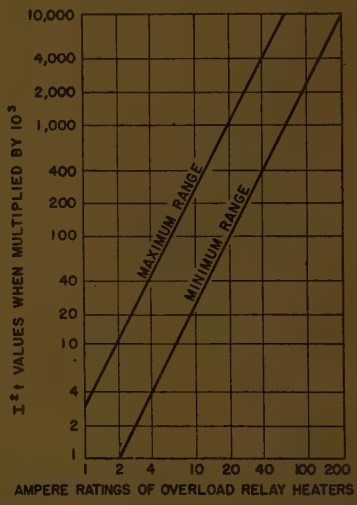


Figure 6. Range of permissible I^2t values which may be found in overload relays

4. If a slow interrupting means is used to protect against short circuits, the starter may be damaged, and the personnel may be injured.

The logical conclusion is that fuses are the only satisfactory available means to protect fully the small-size starters of either the a-c or d-c types.

Reference

1. Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less, B. W. Jones. AIEE Transactions, volume 61, 1942, July section, page 483–7.

A-C Contactors for Aircraft

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Synopsis: Use of a-c power systems on aircraft presents the problem of providing switching means. Contactors with the desired characteristics and suitable size and weight have not been available. Analysis of operating conditions, contactor size requirements, and performance characteristics provide a basis of design of a group of contactors to meet the needs of present and projected aircraft. These designs can meet aircraft size limitations and be capable of handling the circuits under all foreseen normal and emergency requirements.

PRESENT and projected designs of aircraft have electric-system requirements which far exceed in capacity those used in the past. As the airplanes have become larger, the electrically operated auxiliaries have required more and more power. Early airplanes had six-volt d-c systems, but these were quickly found to be inadequate because of excessive drop in the conductors. In order to avoid the use of prohibitively heavy cables, the voltage was increased to 12 and finally to 24 volts. Power requirements have continued to rise since the last increase in voltage, and a point again has been reached where a change in the electric system is considered necessary. A number of possibilities have been suggested.[1] One of these is the use of a-c generators with constant-voltage output at constant frequency with most of the power being used as generated but with a portion of it being rectified for use with d-c instruments and in control circuits. Many points in favor of this system have been advanced.[2]

Use of alternating current makes it necessary to provide suitable switching equipment for controlling the circuits. Relays and contactors used in d-c systems are nearly all single-pole low-voltage devices and are not satisfactory for use on three-phase a-c systems. While all of the devices needed are available in commercial form, their design is such that they are not satisfactory for use in aircraft. Their size and weight is excessive, and they are not designed for use under the wide range of conditions normal to aircraft operation.

Contactor Requirements

Design of the contactors needed involves a study of the factors influencing their operation. These include characteristics of the power system; type of load and duty cycle; as well as temperature, humidity, pressure, vibration, shock, dust, and other conditions to which they might be subjected.

Magnetically operated relays and contactors are used for the following purposes:

1. To shorten the power-carrying lines by running them only to the point of use.
2. To make possible automatic-control functions.
3. To save space at the control stations by the use of small pilot switches.
4. To keep high voltage from control stations by using low-voltage control circuits.

Alternators, driven by the main engines, furnish the principal source of power. The system is 208 volts, 400 cycles, three-phase with grounded neutral. In one proposed arrangement the main bus is a loop with sectionalizing contactors, so arranged that a fault may be isolated and removed with minimum disturbance to the remainder of the system. The alternators feed this loop through latched contactors arranged to trip on a fault in the feeders or in the machines themselves. Loads are fed from the main bus either directly or through branch bus systems. Contactors for connecting the loads are required. In some cases some of the branch systems may be transferred to an auxiliary power supply, in which case transfer switches are used.

If it should prove to be impractical to operate the alternators in parallel on a common bus, it is proposed to supply some of the loads from each of the alternators with provision for transferring in case of emergency. Transfer switches of suitable rating are required to perform this function.

The latched contactors function as circuit breakers operated by separate protective relays not part of the contactor itself. Separation of the protective devices from the main contactor is made in the interests of simplification of the devices and in order to increase interchangeability, since the same contactor may then be used with various circuit protective relays to obtain different types of protection or with manual control for sectionalizing the system.

The greater part of the load consists of motors. All motors are intended to in-



Figure 1. Three-pole single-throw contactor rated 25 amperes, 208 volts, 400 cycles

The covers and moving-contact assembly have been removed to show construction

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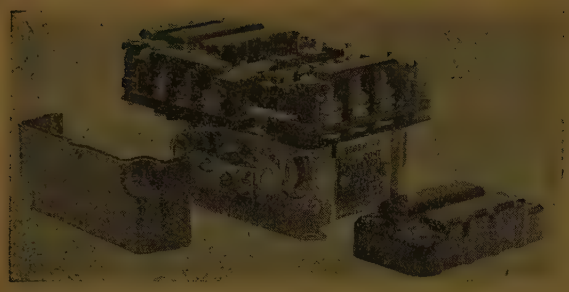


Figure 2. Three-pole double-throw 25-ampere contactor

corporate built-in overload protection, making provision of overload elements as part of the contactor or starting switch unnecessary.

Contactors of several types and ratings are required to perform various functions:

1. Single-pole single-throw for closing and opening single-phase load circuits—25 amperes continuous rating.

2. Single-pole double-throw for transferring circuits—25 amperes continuous rating.

3. Three-pole single-throw for closing and opening three-phase load circuits—5, 25, 50, and 100 amperes continuous rating.

4. Three-pole double-throw for reversing, motor-starting, and transfer of circuits—5, 25, 50, and 100 amperes continuous rating.

5. Three-pole latched for main circuit and alternator disconnects and for circuit protection—120 amperes continuous rating.

In addition to carrying rated full-load current, contactors must be capable of opening and closing the inrush currents which will be obtained with various types of loads. Under emergency conditions, the stalled current of motors may have to be interrupted. Current requirement under these conditions has been established as ten times full-load rating.

Temperature, vibration, and acceleration requirements have been discussed at length in connection with other aircraft equipment. Experience has indicated that in addition greater attention needs to be given to enclosures and protection against the effects of high humidity and the entrance of dust.

D-C Control Circuits

Availability of direct current as a source of power for control circuits makes possible the use of d-c magnets in the contactors. This results in certain advantages:

1. Magnets can be smaller, lighter, and more rugged than if they were made for operation on 400 cycles alternating current.

2. Switches now being used for the control of circuits and contactors in d-c systems can be used, making possible instrument panel layouts similar to those used in d-c systems.

3. Replacement problems are simplified.

4. Many of the present control schemes are usable.

5. High voltages which might be hazardous to operating personnel are removed from the pilot switches.

When automatic control from the a-c circuits is required, small rectifiers may be used to supply the d-c magnets.

Insulation

Use of three-phase circuits requires the use of three-pole contactors, introducing insulation problems in that it is necessary to insulate the a-c and the d-c circuits from each other and from ground, as well as to obtain phase isolation, all in the same device. In commercial practice, the idea of "phase spacing" is frequently synonomous with phase isolation, but in the design of aircraft contactors it is not practical to approach the problem from this point of view. It is not possible to space the poles sufficiently to obtain the required insulation distances without exceeding the space and weight limitations imposed on this apparatus. This problem is complicated further by the need for protection against the entrance of dust, a serious condition when operating on desert airfields, and by the effect of rarified atmospheres on insulation breakdown,[3] since the equipment must be suitable for operation at altitudes up to 40,000 feet. It therefore becomes necessary to obtain phase isolation by providing suitable barriers between poles and to provide an enclosure to prevent the entrance of dust. One of the principal causes of failure in circuit-interrupting devices is flashover between poles due to the presence of ionized gases. There are several ways in which this problem may be overcome:

1. Sufficient space may be provided between poles to limit the ion concentration to a value at which flashover does not occur, a method which is not economical as to space and weight.

2. A sufficiently long path may be provided between poles in which the gases are in contact with cold surfaces so that deionization takes place.

3. The contacts may be placed in closed pockets which prevent the gases from coming into contact with parts of opposite polarity. Provision for venting the gases while maintaining a suitable dust seal may be required.

The last method is the only one which permits the contacts to be sealed to prevent the entrance of dust or other foreign material which might interfere with operation or which would cause insulation failure. It has the disadvantage that, in order to obtain the best space factor in the switch design, the arc is in close proximity with the insulation material. Failure may occur because of insulation breakdown, unless a material is used which has high resistance to arc tracking. Melamine formaldehyde insulation materials have this property and provide a suitable answer to this problem.

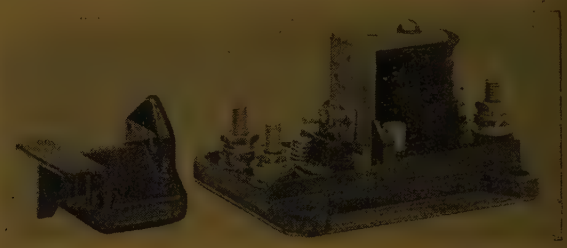


Figure 3. Single-pole single-throw 25-ampere contactor

Arc Interruption

The contactors must be capable of making and breaking the current which may exist under any condition encountered in operation. Under normal conditions, this may be the rated current or less, or the current at the instant the contacts close may be much higher, dropping quickly to the steady current. The contacts therefore must be capable of making ten times the rated current and breaking rated current as a normal condition, that is, for the life of the contactor. Under emergency conditions, the contacts may be called upon to open the same high current for a limited number of operations in order to clear faulty circuits or apparatus. In general, no difficulty is experienced with alternating current in breaking high current at the voltage used on these systems, even with relatively small arc gaps. The gap is determined by the distance required to prevent flashover at the maximum voltage used for insulation testing, in this case 2,500 volts at sea level and 1,000

volts at 40,000 feet altitude. Breaking high current may damage the contacts so as to reduce their life considerably or, in extreme cases, cause failure when the switch is again closed. Closing of the circuit presents a more serious problem, because, when closing on high currents, the contacts are liable to weld. Factors involved in this case are the current on which the contacts close, the material or materials of which the contacts are made, and the amount of arcing obtained during closure—the latter being determined by the mass of the contacts, their velocity and the forces involved: that is, the initial contact pressure and the magnet pull. The design must be such as to obtain the proper relationship of these factors to limit the rebound to as small a value as possible. Use of the contact materials which are not readily welded offers many possibilities, but care must be exercised to keep their resistance low. If the contacts are to be enclosed in relatively tight chambers for obtaining phase isolation and for protection against the entry of foreign materials, the enclosure must be made sufficiently strong to prevent breakage under the relatively high pressures built up when the arc is broken, or some provision must be made to permit the pressure to be relieved.

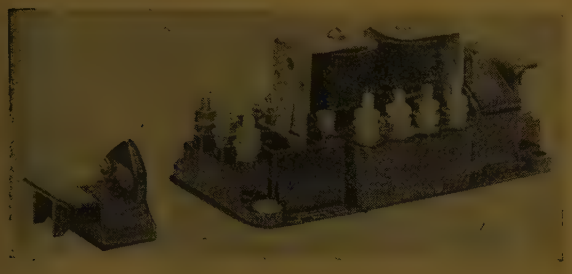


Figure 4. Three-pole double-throw five-ampere contactor

Arrangement

Two general contact structures are available. The moving contacts may be carried on a pivoted arm which is in turn operated by the magnet. In this form, the contacts are generally single break and have flexible connections to carry the current to the moving part. For high current values the connections must be quite large and heavy, so that in order to retain flexibility they are made relatively long. It was thought that a better choice for the larger-size contactors would be the use of bridging-type contacts. Figure 1 shows the arrangement of the contacts and terminals and is typical of three-pole contactors rated 25 amperes and higher. The contact cover and the terminal covers have been removed and the moving contact assembly lifted out. All stationary contacts are fastened to a molded insulating board with barriers to separate the phases. The moving contacts with their springs are carried in a sliding insulator assembly which is guided in a slot formed by the barriers provided between the stationary contacts. The barriers are designed to fit into recesses in the contact cover in such a manner that each contact is an individual chamber formed by the barriers, the slider, and the cover. While the closure between chambers is not absolutely sealed, the connecting spaces are very narrow and of such length that the gases are deionized before passing from one to the other. A seal against the entrance of dust is obtained by use of a compressible heat-resisting gasket between the edges of the cover and the base. Covers are provided over the terminal screws to prevent accidental contact by personnel or by objects which might fall across them. The sliding contact member is actuated by a pivoted lever, one end of which fits into the sliding bar and the other end of which is connected to the solenoid plunger. The contact bar and the solenoid plunger form a balanced structure which has essentially the same mass on each side of the pivot and is therefore unaffected by acceleration forces, making the device insensitive to shock and vibration. On double-throw contactors two similar contactors are mounted side by side on a common base as shown in Figure 2. A pivoted mechanical interlocking bar prevents one plunger from pulling in to close the contacts associated with it if the other plunger is already in its sealed position. Suitable covers with sealing gaskets protect the mechanism from mechanical interference and the entrance of dust.

The smaller-rating contactors are readily adapted to the pivoted armature construction and accordingly are made in this form. Figure 3 is typical of the construction used and shows a 25-ampere single-pole single-throw contactor. A single bridging contact is used in this case, whereas on the three-pole contactor single-break finger-type contacts with flexible connectors are used. Double-throw contactors consist of two contactors mounted on a common base and mechanically interlocked to prevent simultaneous operation. Figure 4 shows a five-ampere three-pole double-throw contactor.

Contacts of the latched contactor are of the bridging type. The arrangement is similar to that used on the larger three-pole switches. The closing magnet consists of a solenoid which pulls the contacts into the closed position where they are held by a latch operating on the lever which moves the contact bar. The magnet coil is disconnected as soon as the contacts reach their sealed position by means of a single-pole double-throw switch operated from the contact bar. Sufficient overtravel of the contacts beyond the normal wear allowance is provided, so that the coil disconnect switch does not operate until after full latch engagement is obtained. This must be done to prevent the possibility of failure of the contacts to latch under low coil voltage. The coil switch also is connected in the circuit of the trip coil so that, when the main contacts are in the closed position, the trip coil may be energized to permit the main contacts to open. Intermittent-duty coils are energized only long enough to close or open the main contacts. In order to prevent "telegraphing," the operating and latch mechanism is arranged to prevent the



Figure 5. Three-pole latched contactor

Rated 120 amperes continuous with 4,000-ampere short-circuit capacity

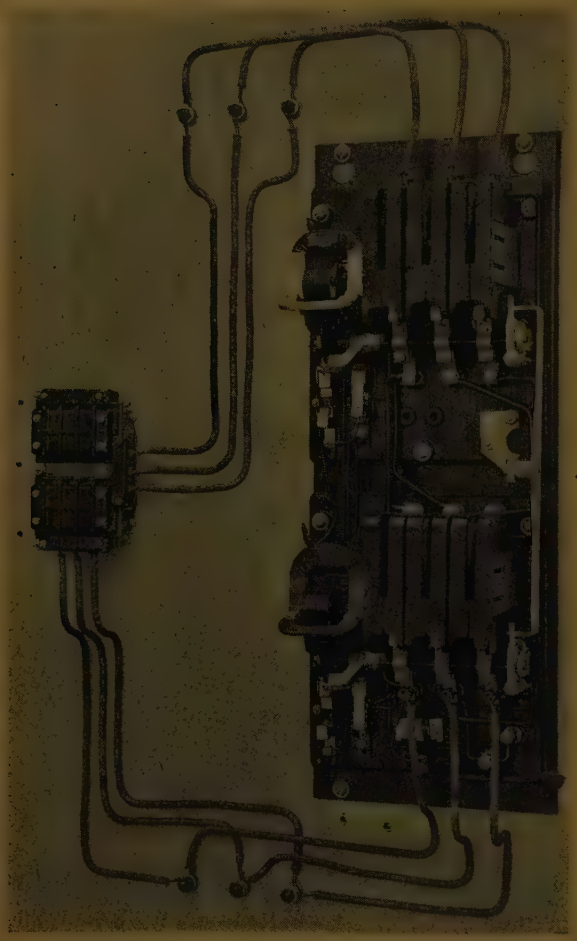


Figure 6. Three-pole double-throw aircraft contactor and comparable commercial contactor

closing magnet from closing the contacts following energization of the trip magnet until the closing-coil circuit has been opened. The contactor is "trip-free," that is, it cannot be held closed when the trip circuit is closed. Additional contacts are provided for electrically interlocking other circuits. Figure 5 shows the construction used in this contactor.

In order to protect other apparatus in the circuit, the fuses and the latched contactors must be co-ordinated in design to provide the proper protection from each under fault conditions. Accordingly, a specification has been established requiring that complete current interruption take place within 0.01 second after the trip impulse has been initiated. This time includes the arcing time and the time required for the contacts to begin to part. The worst condition which might be encountered is on single-phase where the arc might persist for one-half cycle plus the time required for the contacts to reach a gap great enough to prevent the arc from restriking. On 400 cycles this time will be something less than 0.002 second. The contacts must separate in 0.008 second or less after the trip-coil impulse is started. Results indicate that this time can be met satisfactorily.

materials enables contactors to be built which are much smaller and lighter than their commercial counterparts. This is illustrated in Figure 6 which shows a 50-ampere three-pole double-throw aircraft contactor and a commercial contactor of the same rating. It should be noted that the commercial device is open whereas the aircraft contactor is completely enclosed.

Figure 7 shows all of the contactors designed to meet the needs of aircraft a-c electric systems. They include single-pole single- and double-throw contactors rated at 25 amperes; three-pole single- and double-throw contactors for 5, 25, 50, and 100 amperes; and a 120-ampere three-pole latched contactor or circuit breaker. Performance in each case meets the anticipated conditions which might be encountered in a-c systems used on aircraft and, with the exception of life, which for aircraft applications is much lower than required in commercial applications, either meets or exceeds the conditions required on the equivalent commercial contactor.



Figure 7. Various sizes and types of contactors required on large aircraft with a-c electric systems

Summary

Requirements for aircraft contactors may be summarized briefly as follows.

1. Small size and low weight.
2. Resistance to vibration and shock incident to their use on aircraft.
3. Ability to operate under a wide range of temperature, humidity, and pressure.
4. Enclosures to exclude dust from contacts and mechanism.
5. Ability to handle normal and emergency load conditions.
6. Confinement of arc within the device.

Analysis of these requirements and design to obtain maximum utilization of

References

1. Basic Considerations in Selection of Electric Systems for Large Aircraft, W. K. Boice, L. G. Levoy, Jr. AIEE Transactions, volume 63, 1944, June section, pages 279–87.

2. Why Tomorrow's Planes Will Have A-C Systems, T. B. Holliday. *Electrical Manufacturing*, volume 32, July 1943, page 88.

3. Potential Breakdown of Small Gaps Under Simulated High-Altitude Conditions, M. J. De Lerno. AIEE Transactions, volume 63, 1944, March section, pages 109–12.

Industrial Control—Shunted-Armature Connection for a D-C Shunt Motor

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THERE are many operations in applications where d-c shunt motors are used where it is necessary to slow down the rotating system in order to perform certain functions. Many typical examples are found in machine-tool work, such as cross-feeding of a tool, milling-cutter slow-down for sharp corners, accurate positioning of tool to work, slow-down of a grinding wheel for dressing. Other examples are found in the slowing down of a printing machine for the purpose of matching colors and occasionally to obtain a threading speed in a textile range drive.

There are three common methods by which these slow-down speeds may be obtained:

1. Resistance inserted in series with the armature is a method of slowing down a motor but has the disadvantage that the speed will vary considerably for slight changes in the torque demanded by the load, so that this method has poor speed regulation.

2. Variable-voltage control is a circuit in which the generator field is adjusted to vary the armature voltage, while the motor field is left constant. This method gives a range of speeds up to basic speed with good speed-torque regulation and good efficiency but is inherently too costly for intermittent applications such as were mentioned previously.

3. If resistance is put in parallel with the armature in addition to series line resistance, a method of control known as the shunted-armature connection is given. This method provides good speed regulation at low initial cost and is ideally suited for short-time or intermittent slow-downs, despite the decreased efficiency due to losses in the shunt resistance. The method is in effect a potentiometer connection (see Figure 1).

Scope of Paper

This paper presents a simple graphical method of obtaining the required shunt and series resistances for the shunted-armature connection to obtain any desired speed at a specified value of torque and also to give a predetermined no-load speed. An analytical verification of the graphical method is also presented.

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The method is outlined for the cases of a constant-speed motor and an adjustable-speed motor.

Nomenclature

I = line current in amperes
V = line voltage in volts
R = series resistance in ohms
S = shunt resistance in ohms
E = back electromotive force in volts
I_s = current through shunt resistance
I_a = armature current in amperes
R_a = armature resistance in ohms
N = speed in rpm
E_o = back electromotive force in volts when $I_a = 0$

All lower-case letters refer to the per unit value of the aforementioned quantities.

T = torque in foot-pounds
T = per unit torque

The subscript FL refers to the full-load condition in the motor. The subscript NL refers to the no-load or running-light condition when full voltage V is applied.

$R_o = V/I_{FL}$ = base or normal ohms
n_o = rpm in per unit when $i_a = 0$

Analysis

Constant-Speed Motors

Application of Kirchoff's laws to the circuit shown in Figure 1 yields the equations,

$$I = I_a + I_s \quad (1)$$

$$V = IR + E + I_a R_a \quad (2)$$

and

$$I_s S = E + I_a R_a \quad (3)$$

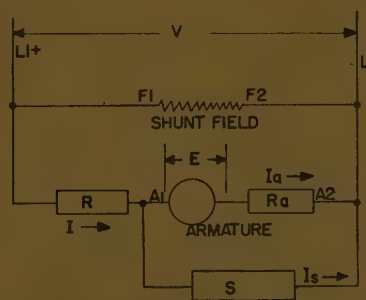


Figure 1. Elementary diagram for the shunted-armature connection showing shunt and series resistance

If equations 1, 2, and 3 are solved simultaneously, a relationship between the armature current and the counter electromotive force is given as a function of the circuit resistances. The resulting equation is

$$I_a\left[R_a\frac{(S+R)}{S}+R\right]=V-E\frac{(S+R)}{S} \quad (4)$$

The intercepts of equation 4 are given by putting $I_a=0$ and $E=0$. They become:

$$I_s=\frac{V}{R_a\frac{(S+R)}{S}+R} \quad (4a)$$

and

$$E_o=V\frac{S}{S+R} \quad (4b)$$

Equation 4 represents a straight line on the axes of armature current as the ordinate and counter electromotive force as the abscissa.

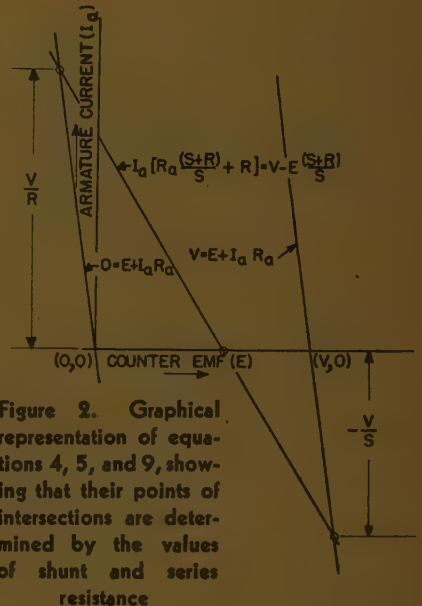


Figure 2. Graphical representation of equations 4, 5, and 9, showing that their points of intersections are determined by the values of shunt and series resistance

Let equation 4 be solved simultaneously with the motor equation,

$$V=E+I_aR_a \quad (5)$$

to find the point of intersection.

Substitution of $E=V-I_aR_a$ from equation 5 into equation 4 gives

$$I_a\left[R_a\frac{(S+R)}{S}+R\right]=V-(V-I_aR_a)\frac{(S+R)}{S} \quad (6)$$

which reduces to

$$I_aR=V\left[1-\frac{S+R}{S}\right] \quad (7)$$

so that, at the point of intersection,

$$I_a=-V/S \quad (8)$$

Now, if equation 4 is solved simultaneously with the straight-line equation,

$$0 = E + I_a R_a \qquad (9)$$

the point of intersection becomes

$$I_a = V/R \qquad (10)$$

This shows that, if equations 4, 5, and 9 are plotted on the same co-ordinates of I_a and E, their points of intersection are determined by the values of shunt and series resistance. These relationships are shown in Figure 2.

Per Unit System

Let line voltage V be 100 per cent or base voltage, full field current I_{FL} be

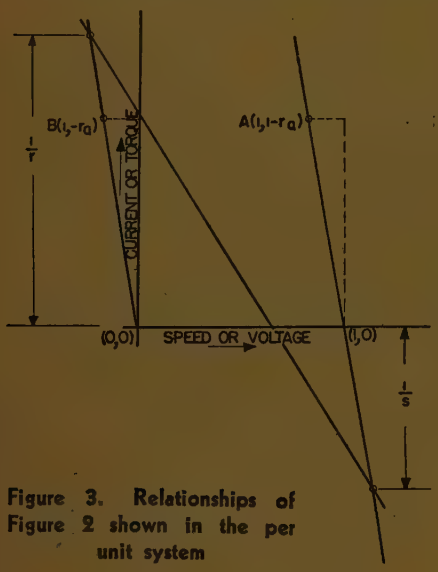


Figure 3. Relationships of Figure 2 shown in the per unit system

base current, and no-load speed N_{NL} be base speed.
Since

$$T = c\phi I_a \qquad (11)$$

and

$$E = K\phi N \qquad (12)$$

then, by eliminating ϕ from equations 11 and 12,

$$T = \frac{c}{K}\frac{EI_a}{N} \qquad (13)$$

and

$$T_{FL} = \frac{c}{K}\frac{E}{N} I_{FL} = \frac{c}{K}\frac{V}{N_{NL}} I_{FL} \qquad (14)$$

since the back electromotive force equals line voltage at no-load speed. Therefore, base torque is full-load torque which is consistent with the aforementioned base quantities of voltage, current, and speed.

In this per unit system,

Per unit volts $e = E/V$

Per unit current $i = I/I_{FL}$

Per unit speed $n = N/N_{NL}$

Per unit torque $T = T/T_{FL}$

Also,

Normal ohms $R_o = V/I_{FL}$

so that

Per unit ohms = ohms/R_o

If equations 11 and 12 are put into the per unit system,

$$T = i_a \qquad (15)$$

and

$$e = n \qquad (16)$$

Figure 3 is obtained by transposing Figure 2 into the per unit system and using torque and speed as co-ordinates alternately with current and voltage as permitted by equations 15 and 16.

Equations 5 and 9 in the per unit system become

$$1 = e + i_a r_a \qquad (17)$$

$$0 = e + i_a r_a \qquad (18)$$

If i_a is put equal to unity in equations 17 and 18 to find the co-ordinates of two points A and B in Figure 3, A $(1, 1-r_a)$ and B $(1, -r_a)$ are yielded, so that equations 17 and 18 are determined only by the value of internal resistance in per unit at the unity-current point.

Equation 4 in per unit is

$$i_a\left[r_a\frac{(s+r)}{s}\right] + r = 1 - e\frac{(s+r)}{s} \qquad (19)$$

When i_a equals zero, equation 19 becomes

$$e_o = \frac{s}{s+r} \qquad (20)$$

From equation 4b:

$$\frac{S}{S+R} = \frac{E_o}{V} = \frac{E_o}{E_{NL}} = \frac{N_o}{N_{NL}} \qquad (21)$$

Now,

$$\frac{E_o}{V} = e_o$$

and

$$\frac{N_o}{N_{NL}} = n_o$$

so that $e_o = n_o$ from equation 21, and the intercept on the x axis is

$$n_o = s/(s+r)$$

Equation 19 is then easily plotted from the given data of one required speed-torque point and the no-load speed.

Graphical Solution

A sheet of graph paper is laid out with ordinate as current and abscissa as speed varying from zero to one. At the unity-current point, the points B and A (Figure 3) are plotted by knowing the per unit internal resistance of the motor. Through these points and the (0, 0) and (1, 0) points, respectively, straight lines are drawn. From the given data of a speed-torque point (i_a, n) in per unit and the per unit no-load speed required (n_o), a straight line is drawn. The two points of intersection of these three straight lines yield the reciprocals of the shunt and series resistance in per unit.

Example

A machine driven by a ten-horsepower 230-volt 1,150-rpm d-c shunt motor must be slowed down for a short time, so that the shunted-armature connection is indicated. The stated requirement is that the slow-down shall be 45 per cent of the full-load motor speed, and that the speed regulation shall be such that the speed will not exceed 66⅔ per cent of the full-load speed, if all load is removed from the motor shaft. It is given that the load demands 34 per cent of full-load torque, referred to the ten-horsepower motor, at 45 per cent of the motor-rated speed.

Since 100 per cent or per unit speed has been defined as the no-load speed of the motor, the full-load speed can be obtained by subtracting the per unit internal drop from unity. For this size motor the internal resistance is usually taken as ten per cent normal ohms, so that points A and B can be plotted on Figure 4.

The lines drawn through these points and the points (1, 0) and (0, 0) represent the motor characteristics with no added shunt or series resistance.

The 45 per cent speed referred to the no-load speed becomes:

$0.45 \times 0.9 = 0.405$

while the 66⅔ per cent speed becomes:

$0.66^2/_3 \times 0.9 = 0.6$

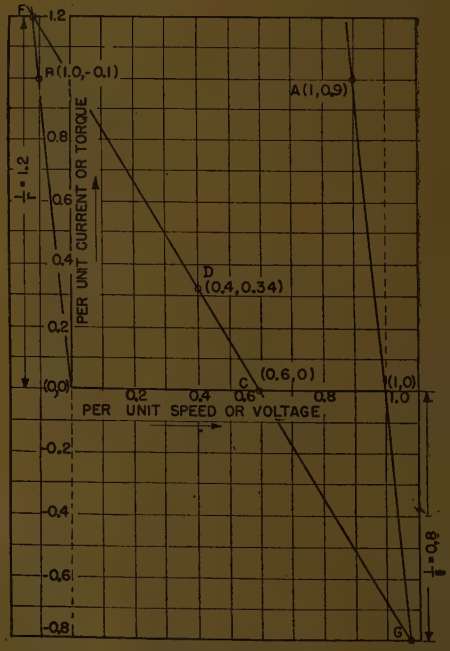


Figure 4. Graphical solution of example presented in the text

This translation of reference speeds from the full-load to the no-load base permits the plotting of the operating points in the per unit system as points C and D, Figure 4. A line drawn through these two points and extended will intersect the motor curves at points F and G. For this particular solution, the ordinates of the points of intersection are $(+1.2)$ and (-0.8). The reciprocal of these two values gives the per unit ohms of the series and shunt resistances, respectively.
Therefore,

$r = 1/1.2 = 0.833$
$s = 1/0.8 = 1.25$

With 100 per cent ohms equal to line voltage V divided by full-load current I_{FL}, the necessary resistances to meet the preceding requirements are:

$R = 230/39 \times 0.833 = 4.91$ ohms
$S = 230/39 \times 1.25 = 7.37$ ohms

Adjustable-Speed Motors

A similar graphical construction can be used to determine the correct resistances for use with adjustable-speed motors. For these motors, load data are usually given with reference to the highest horsepower which occurs at the highest speed. To obtain speed control between highest- and lowest-rated speed, the field current is varied. For speeds below the lowest-rated speed, the armature-shunt connection is used. Therefore, if speeds are referred to this lowest-rated speed as base speed and base torque is full field torque, the graphical solution is identical with that for constant-speed motors. For example, assume that an application using a $7^1/_2$/10-horsepower 500/1,000-rpm, 230-volt d-c motor must be controlled to give 250 rpm at 80 per cent torque. This latter figure refers to the high speed or weak field torque and is therefore 40 per cent of full field torque. Accordingly, the point which corresponds to D in Figure 4 is plotted at

$$\frac{250\ (0.9)}{500} = 45 \text{ per cent speed and } 40 \text{ per cent torque}$$

Specification of the required speed with no load on the motor shaft, or specification of the speed regulation determines the second point, so that the straight line can be drawn as before.

Conclusion

In many applications the torque demanded by the load is not known very accurately, although the required speed is well defined. The application engineer can compensate for the inaccuracy with which the load torque is known by using the shunted-armature connection and by so choosing a value of shunt resistance to give a good speed regulation. Without a graphical solution, simultaneous equations must be solved analytically. The method previously presented is particularly useful because of its simplicity in obtaining a particular solution as well as determining families of curves that show the effect of shunt and series resistance on the speed-torque characteristics.

Possibilities of Heat Pumps for Heating Homes

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Synopsis: Heat pumps are refrigerating systems in which the principal interest is in the heat given off by the condenser. A portion of this comes from the electric input to the compressor and the remainder from cooling an external source of heat, usually outdoor air. Heat output is several times the equivalent of electric input, and these systems offer possibilities of fuel economy beyond anything possible with fuel-burning equipment. These systems can be used for all-year air conditioning, but the heat requirement dictates larger equipment than necessary for cooling only. Electric-load requirements are attractive, but further development is necessary and will be dependent on the promotion of summer air conditioning for homes.

SOME 92 years ago Lord Kelvin first called to our attention the fact that refrigerating systems are reversible, and that they have some unusual and interesting characteristics when operated as heating systems. Interest in the idea remained very low until about twenty years ago, when the development of the domestic electric refrigerator and commercial refrigeration for food preservation began to make engineers think of summer air conditioning for homes and buildings as something more than just a far-off dream. Since then interest, as judged by technical publications, has steadily increased, and a few actual installations have been made. Practically all of the installations were made before the war and nearly all of them are in successful operation today.

To predict that in the next ten years a substantial proportion of our homes will not be heated by heat pumps seems rash, in view of the rapid development we have already seen in other types of equipment for the home. Therefore, it would appear to be of interest to analyze the prospects as they exist today somewhat sympathetically, even though the way in which widespread use may be obtained is not yet clear.

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Description of a Heat Pump

The heat which a domestic refrigerator absorbs from its cabinet through its evaporator does not just disappear. By means of a mechanical compressor, the temperature of the absorbed heat is raised to the point at which the heat can be transferred to the air outside of the cabinet through its condenser. In so doing it adds to the absorbed heat the heat equivalent of the work done by the compressor, and the sum of the two quantities of heat result as heat added to the room air surrounding the cabinet. When the reason for using the refrigerator is to take advantage of the lowered temperature inside the cabinet, it is called a refrigerator. When the primary object is to heat the surrounding room, it is called a reversed-cycle refrigerator or a heat pump. The type of equipment used in either case is the same. In the case of the heat pump heating a home, instead of placing the evaporator inside of an insulated cabinet, it is more effective to place it in the outdoor air or in a supply of water that can be cooled, and the condenser is usually in the form of a finned coil, with a fan blowing the heated air from the coil through ducts to the house. In any case, the heating of the house air and the simultaneous cooling of the outdoor air or water supply are fundamental to the heat pump, even though the outdoor air being cooled may be at a temperature well below zero.

Figure 1 shows a simplified schematic diagram of a direct-expansion heat pump. When operating in the winter time for heating, air from the house is forced through the condenser, where it is heated sufficiently to satisfy the heat loss of the house. Air from outdoors is forced through the evaporator, where it is cooled sufficiently to provide its contribution of heat to be transmitted to the house.

The liquid refrigerant inside the evaporator is boiled by the heat from the outdoor air at a boiling pressure and temperature sufficiently below the outdoor-air temperature to effect the necessary heat transfer. The compressor draws the refrigerant gas from the evaporator and discharges it to the condenser at a high enough pressure and temperature so that it will condense and transfer its heat to the outgoing house air stream. The expansion valve maintains the pressure difference between condenser and evaporator by regulating the flow of refrigerant through the system. To use this same system for cooling the house in summer, the outdoor air and house air streams are interchanged with dampers, or an equivalent result is accomplished by other means.

If all losses are neglected, the heat output from the condenser of this system is equal to the heat input to the evaporator plus the electric input to the compressor motor. Since the outdoor air is free, the efficiency of the system is the heat output divided by the heat equivalent of the electric input, and because this is always greater than 100 per cent, it is called a coefficient of performance. The electric input multiplied by the coefficient of performance gives the heat output to the house.

Efficiency

The relative proportion of the heat obtained from electric input and from outdoor air depends primarily on the indoor and outdoor temperatures. This is quite different from a fuel-burning furnace, in which efficiency is substantially independent of outdoor temperature. As in the case of a steam-power plant, the heat pump is a heat engine absorbing heat at one temperature level and discharging it at another; the principal difference is that a steam-power plant delivers mechanical work from the engine, whereas the heat pump absorbs it. Both follow a Carnot cycle in the theoretical ideal, and the theoretical ideal efficiencies are similar in form, that for the heat pump being

$$\text{Coefficient of performance} = \frac{T_1}{T_1 - T_2}$$

where T_1 is the house or condenser temperature (both being the same in the ideal) and T_2 is the outdoor or evaporator temperature. Temperatures in this case must be measured on the absolute scale. Curve *A* in Figure 2 shows the coefficient of performance for a perfect Carnot engine heat pump maintaining 70 degrees Fahrenheit in a house at various outdoor temperatures. It will be noted that with a heat pump at 20 degrees Fahrenheit outdoor temperature it is theoretically possible to obtain 10.6 times as much heat in the house as would be possible by using

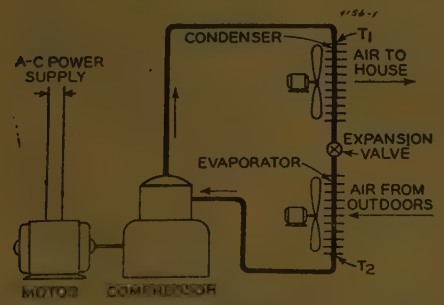


Figure 1. Schematic diagram of elements of a heat pump

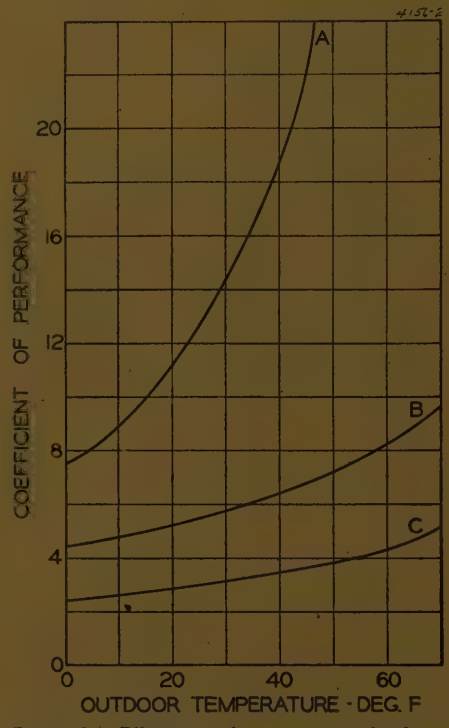


Figure 2. Efficiency characteristics of a heat pump maintaining a house at 70 degrees

the same power input in electric resistance heaters. In addition to a perfect heat engine this would require condenser and evaporator heat-transfer surfaces sufficiently large to transfer the heat without appreciable temperature differences, and correspondingly large air flows in the house and outdoor air circuits. Sizing the heat-transfer surfaces and air flows to values that would be considered practical in a summer air-conditioning system of similar proportions would give curve *B* in Figure 2, where the refrigerant condensing temperature has been increased and evaporating temperature decreased a total of 60 degrees Fahrenheit. This would permit delivery of heated air to the house at a temperature between 105 to 110 degrees Fahrenheit, which is considerably lower than would be used with a fuel-burning warm-air heating plant, but not enough lower than usual discharge temperatures from a normal year-round residential air-conditioning system to have a prohibitive effect on the cost of fans and duct work.

Curve *C* in Figure 2 shows coefficients of performance that can be obtained with refrigeration equipment available today. This curve differs from curve *B* in that proper allowance has been made for the use of Freon 12, instead of a perfect refrigerant, for mechanical and other inefficiencies of equipment and for power required by auxiliaries such as fans. The performance indicated by this curve is believed to have been reached by installations now in operation using outdoor air as a source of evaporator heat. It will be noted that in climates such as Pittsburgh or Philadelphia, where less than three per cent of the requirements of the heating season occur at below 20 degrees Fahrenheit outdoor temperature, an average coefficient of performance for the whole heating season of better than three to one can be reasonably expected.

Fuel Economy

At about 52 degrees Fahrenheit outdoor temperature, the realizable coefficient of performance reaches four to one. The best steam-power generating plants today have an over-all thermal efficiency of approximately 34 per cent. Hence, even with a 15 per cent allowance for transmission losses between the generating station and the home, the heat pump will deliver at the 52 degrees Fahrenheit outdoor condition 116 per cent of the heat that was in the fuel burned at the generating station for supplying energy to the heat pump. Under favorable conditions the heat pump can pick up from its outdoor air more than all of the losses from the system, including the heat rejected to its cooling water by the generating station. At first glance this might appear to offer encouragement to the seekers of perpetual motion machines, but a second glance reveals that, while we have increased the number of Btu available at the heat-pump condenser over what we started with in the fuel, its temperature level would not be attractive to the central-station operator as a source of mechanical work. The heat pump effects a favorable exchange of high-grade electric energy for a greater quantity of low-grade heat energy, and it is well known that the same favorable rate of exchange cannot be obtained in the opposite direction. However, the heat output of the heat pump is quite suitable for heating homes, and even at a three to one season-average coefficient of performance and 15 per cent electrical transmission losses, it gives 87 per cent over-all fuel economy. This is substantially better than can be obtained with the best domestic automatic fuel-burning equipment, and more than 50 per cent better than a good hand-fired domestic coal furnace will give. As time goes on undoubtedly more efficient central stations and more efficient heat-pump equipment will be developed, and it is not impossible to imagine that heat pumps may some day be required just from the point of view of conservation of our national fuel resources. Curve *A* in Figure 2 shows plainly that the develop-

ment of this type of system is not limited to 100 per cent fuel economy.

Of course, fuel utilization efficiency is not so interesting to the operator of a heat pump as is the annual operating cost in dollars. Here there can be a considerable variation of opinion, because there are no reliable statistics available on the actual average operating efficiencies of the various kinds of fuel-burning domestic heating plants. If we assume hand-fired coal furnaces at 55 per cent efficiency, and oil and gas automatic furnaces at 75 per cent efficiency, they would compare as follows with a heat pump averaging 3.0 coefficient of performance and using power at one cent per kilowatt hour. The heat pump would have the same operating cost as:

1. Coal at $14 per ton.
2. Oil at ten cents per gallon.
3. Manufactured gas at 40 cents per thousand cubic feet.
4. Natural gas at 80 cents per thousand cubic feet.

This is approximately the picture as it exists today for a heat pump constructed with general-purpose refrigeration equipment in a climate such as Pittsburgh and drawing its evaporator heat from the outdoor air. It can be improved substantially by circulating more house air at a lower temperature and using larger heat-transfer surfaces, thus increasing the first cost and departing from present limits of practice in year-round air-conditioning systems. It can be improved, also, by further development of the equipment, or by finding a higher-temperature source of heat for the evaporator than outdoor air.

The possibilities of higher-temperature heat sources have been well explored but possibly not exhausted. Most city water supplies are at a high enough temperature in winter to be usable as a heat source, but the cost of the water usually will exceed the power saving. The use of the heat of fusion of water by freezing ice presents the problem of disposing of tons of ice per day per home in very cold weather. The use of well water, where available for the cost of pumping, can give average coefficients of performance of higher than four to one, and has been used very successfully, but since the availability in the United States is very limited, this furnishes only a local solution to the problem.

Output Characteristics

Curve *A* in Figure 3 shows the output characteristic, calculated from test data, of a heat pump using a constant-speed 7½-horsepower condensing unit with an outdoor air evaporator. In spite of the fact that the coefficient of performance or efficiency falls off at low outdoor temperatures, the power input, as shown in curve *B*, also falls off because of the decrease in circulation of refrigerant at low suction densities as the evaporator temperature and pressure fall. Curve *C* shows the requirements of a house needing 85,000 Btu per hour at zero degrees Fahrenheit outdoor temperature, which would be a three-bedroom house of average proportions, well insulated, in a climate such as Pittsburgh or Philadelphia. Such a house would be in the $8,000 to $10,000

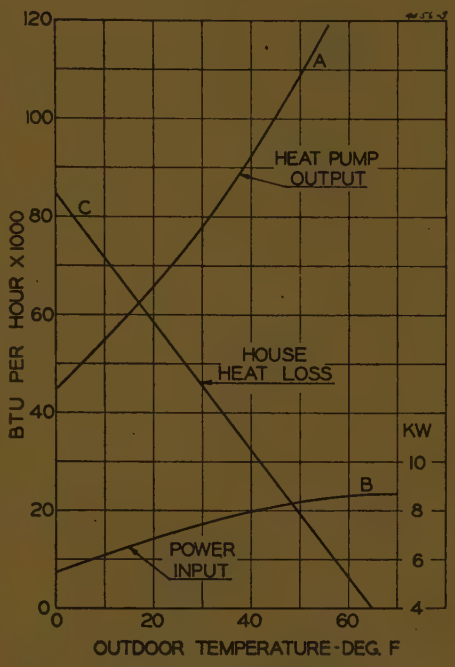


Figure 3. Output characteristics of a heat pump with constant-speed 7½-horsepower compressor

price range at prewar levels. The 7½-horsepower heat pump has capacity to heat this house down to 15 degrees Fahrenheit outdoor temperature. Rather than use a larger heat-pump system to take care of temperatures below 15 degrees Fahrenheit, a storage scheme could be used. The excess capacity of the system at temperatures above 15 degrees Fahrenheit would be used to heat water in a storage tank to a temperature of 100 to 110 degrees Fahrenheit, and when the outdoor temperature falls below 15 degrees Fahrenheit this reservoir of heat can be drawn upon to maintain the house. The size of storage tank that will be required will depend upon the severity of the longest cold spell below 15 degrees Fahrenheit to be expected at the location of the heat pump. In Pittsburgh, the size of the tank would be of the same order of magnitude as the fuel-oil storage tank that might be used if the same house were heated with an oil-burning furnace.

Sources of auxiliary heat other than storage also can be used where advantageous. The city water-supply system is a convenient source in this case, because the total usage will be small enough so that the cost is not important, and the demand would come at a season when practically all city water systems have ample surplus capacity. A small auxiliary evaporator cooling this water would be called upon for less than three per cent of the integrated heating-season demand in the Pittsburgh climate.

From curves *A* and *C* in Figure 3 it is obvious that the constant-speed compressor gives far too much capacity at the higher outdoor temperatures. This is not desirable in any warm-air heating system, where it is likely to cause short cycling and excessive temperature fluctuation in the house. This type of system, therefore, requires a multispeed compressor motor or other means of capacity reduction, with the necessary controls to modulate the system output to the approximate requirements of the house.

A requirement of 85,000 Btu per hour to heat a house at zero degrees Fahrenheit is composed of conduction through walls and windows, and infiltration of outside air. In summer the indoor–outdoor design difference is typically only 16 degrees Fahrenheit, which reduces the Btu load to 19,400 Btu per hour. To this must be added the requirements for increased fresh-air supply, people, lights, solar radiation, and latent cooling of the fresh air and people. All of these can vary widely, but it can be said that the majority of insulated houses of this size can be air conditioned satisfactorily with a three-horsepower system giving from 35,000 to 40,000 Btu per hour under hot-weather conditions. Some houses with large glass areas exposed to the sun may require five-horsepower systems. This is typical of most parts of this country, and brings out the fact that few summer air-conditioning systems, designed as such for residential use, can be converted to heat pumps without adding capacity. Conversely, the cost of installing a heat pump in a residence involves more than just the extra cost of controls and reversing mechanism applied to a summer air-conditioning system. Whereas the capacity of the condensing unit may have to be increased in the ratio of 7½ horsepower to three horsepower, the increase in size of heat transfer and air-handling equipment generally will be considerably less.

Initial Cost

Any authoritative discussion of installed prices of heat pumps would be hopelessly beyond the scope of a short exposition such as this. It would involve the effects of local codes and building-trades practices, methods of selling and installing, selection of installation materials, and all the other things that cause large variations in prices between one locality and another, and between two installations in the same locality when the system is assembled from individual pieces specifically engineered in the field

to the requirements of a particular house. Nonetheless, some highly approximate estimated figures might be of value as an indication of relative costs compared to those of conventional systems. A warm-air oil-fired furnace for a new well-insulated house requiring 85,000 Btu per hour might be sold for $700 today in a medium-size city. This would include uninsulated duct work, grills, oil tanks, and all necessary items for a complete installation, with a minimum of accessories. If this installation had been purchased as a year-round system, the duct sizes would have been somewhat larger, parts of the duct work would have been insulated, and the added equipment might have included a three-horsepower condensing unit, an evaporator coil mounted in the supply-duct work, an evaporative condenser, refrigerant piping, cooling controls, refrigerant, and numerous smaller items. If the same method of estimating is used as in the case of the simple furnace, this system complete would be sold for $1,650. Again, if the same basis for estimating is used, a 7½-horsepower heat pump with two-step capacity modulation, outdoor-air evaporator supplemented with a 1,000-gallon insulated water-storage tank for peak load, and automatic controls except for manual changeover from heating to cooling, would be sold for $2,600. The heat pump might in some cases be entitled to a credit of approximately $150 because it would not need a chimney. These figures are not represented as being either average or typical, but are prices that might be encountered in a climate such as Pittsburgh if the standard heating and refrigeration equipment available today were used. The cooling equipment would be selected and installed by the contractor in the field from various designs and sizes of compressors, fans, heat-transfer surfaces, and so forth. Obviously, whenever a volume is reached sufficient to justify pre-engineered packaged designs assembled in factories, the price of the complete system to the user would be considerably altered.

Energy Usage

Figure 4 shows the effect on electrical demand of installation of various types of systems in the typical six-room house in a Pittsburgh climate. Curve *A* is for reference, and shows the average hourly demand of an all-electric home, containing all electrical appliances except heating and air conditioning. It averages 360 kilowatt-hours per month total usage, and it will be recognized readily that few homes of this size actually approach or seldom exceed this figure. Curve *B* shows the demand of a three-horsepower summer air-conditioning installation in the same house on a hot day, when the outdoor temperature reaches 96 degrees Fahrenheit in the afternoon. Curves *C* and *D* show hourly demand curves for a heat pump in this house on a mild day and on a zero degree Fahrenheit day, respectively. If this house were heated with electric resistance heaters, the peak demand of the heating system would be 25 kw.

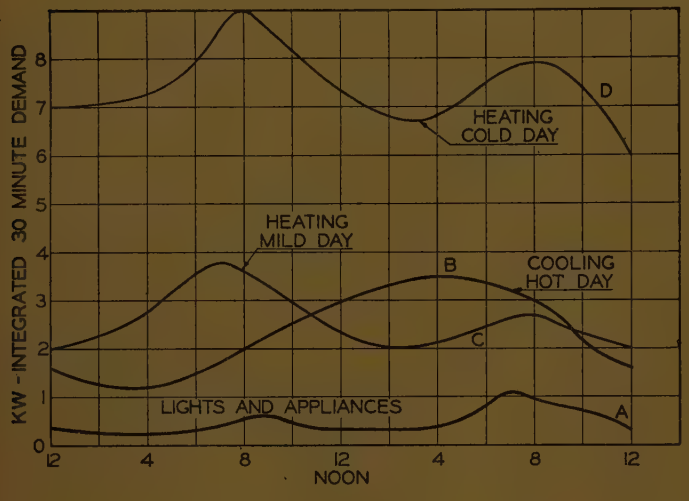


Figure 4. Kilowatt-demand requirements of a heat pump at various conditions

Estimating the average annual energy consumption of a heat pump is obviously a tedious undertaking, because the coefficient of performance is a function of outdoor temperature and the result is doubly dependent on the weather. However, approximate calculations for the average Pittsburgh climate, including both winter heating and summer cooling, give a total usage of 17,000 kilowatt-hours per year. If Pittsburgh represents an average climate in the United States, for which reason it was chosen here, then, even after all homes have reached a lighting and appliance usage of 4,000 kilowatt-hours per year, it would require installation of heat pumps in only 25 per cent of them to double the domestic electric load.

Conclusions

Quite apart from the residential possibilities of heat pumps, these systems should receive greatly increased attention in the postwar period because of their commercial and industrial applications. Many industrial applications will be found where there will be an economically advantageous use for both the heating and cooling that these systems provide simultaneously. Many industrial and commercial applications will be found where summer air conditioning is necessary, and where a high internal cooling load will make the cooling equipment adequate as a heat pump without increasing its size. Again, the availability of well water or process waste heat at a temperature too low for space heating but above outdoor temperature may make the heat pump, as an addition to a cooling system, far more attractive economically than in the case of the residential application.

If the increased interest is to carry over into the residential field and result in any widespread use, it is clear that further development work will have to be done. At present costs and performance it can be predicted safely that usage will be very limited except possibly in southern California and Florida, where the climate is much more favorable than in Pittsburgh. However, from the standpoint of first cost, it is also clear that the heat pump, when developed, will be attractive principally to those people who want and can afford to pay for over-all summer air conditioning of their homes and not for just one or two rooms. To date this market might be described as extremely small but promising. Equipment consisting of an electrically driven summer air-conditioning plant in combination with a fuel-burning furnace has reached a good stage of development, and application of it to residences is well past the pioneer stage. However, it has not received the promotion that manufacturers and utilities gave to the household electric refrigerator, for example, when it was introduced. Certainly endeavors will be made to promote it in the postwar period. The future development of the heat pump for residential use probably will depend upon the relative success or failure of these endeavors.

References

1. The Heat Pump—An Economical Method of Producing Low-Grade Heat from Electricity, T. G. N. Haldane. *Journal* of the Institution of Electrical Engineers, volume 68, June 1930, pages 666–675.

2. Application of Refrigeration to Heating and Cooling of Homes, A. R. Stevenson, Jr., F. H. Faust, E. W. Roessler. *General Electric Review*, volume 35, number 3, March 1932, pages 145–53.

3. An All-Electric Heating, Cooling, and Air-Conditioning System, Philip Sporn, D. W. McLenegan. *Heating, Piping and Air Conditioning*, August 1935.

4. The Heat Pump, Philip Sporn, E. R. Ambrose. *Heating and Ventilating*, January 1944.

Modern Cargo-Winch Control for Use on Victory Ships

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THE electric-driven cargo winces used to load and unload a modern cargo vessel are required to give fast efficient operation. Wartime conditions have made the time for loading and unloading cargo more important than ever. In addition to the need for optimum operating characteristics, the war has also brought about changes in the construction and assembly of the winches and their associated control. Every square foot of deck space is needed to carry cargo to the fighting fronts, and the space allotted to the winch must be kept to a minimum. In the past the winch, the control panel, and the resistor had been furnished separately, then mounted and wired on the ship. The time to install all this equipment on the vessel is now too long to keep pace with the accelerated shipbuilding program. The problem is being solved by furnishing the winch, the control, and the resistor, as a unit. This unit is interconnected so that very little wiring must be done on the ship and the installation time is materially reduced (see Figure 1).

In this paper the operating characteristics required for an electric-driven cargo winch will be discussed. The manner in which these characteristics may be obtained by the use of a properly designed motor control, and the problems encountered in incorporating control equipment in an enclosure mounted on the winch will be outlined.

Control Requirements for the Cargo Winch

Most cargo is handled by using two winches operating on a burtoning rig (see Figure 12 and appendix). This setup results in one winch hoisting the load and transferring the load to the other winch which lowers it. Either winch will hoist a load of a ton and a half at a speed of 290 feet per minute. Desirable operating characteristics for these winches are:

1. Speed characteristics suitable for use on a burtoning rig. The speed of one winch at light load must be matched by the other winch operating at full load.

2. Rapid acceleration and deceleration of the load to decrease the load travel time. Acceleration of the load is an important factor in placing the load under the hatch.

3. Extremely low speed, from 10 to 20 feet per minute, lowering to land heavy loads. This feature is important when lowering a load into a lighter. The lighter may be moving up to meet the load, by the action of a wave, which effectively increases the landing speed.

4. High hoisting and lowering speeds with an empty hook, approximately 400 feet per minute to give fast loading cycle.

5. Intermediate hoisting speeds at light loads are necessary to transfer the load smoothly from one winch to another. These intermediate hoist speeds also give better control of an empty hook.

6. Good jogging characteristics.

The desired cargo-winch operating characteristics may be obtained by the use of a d-c compound-wound motor. The proper connections of the motor to the line on the various operating points must be provided by the control equipment. These power-circuit connections for each point are shown, and the reasons why they are used are explained in the following.

First-Point Hoisting

This is a low torque point used to take the slack out of the cable and gears. The motor is connected to the line through a relatively large value of resistance, which limits the torque that may be produced on this point. In order to reduce the light-line speed a resistance is connected directly across the motor, inside the line resistance, which limits the no-load armature voltage. The circuit is shown in Figure 2.



Figure 1. One-unit cargo winches installed on board ship

Second-Point Hoisting

A relatively low speed with full load is needed on this point. This may be obtained by using the same circuit as used on the first point and merely reducing the line resistance. This circuit gives a little greater light-line speed and produces enough torque to hoist full loads slowly. The circuit is shown in Figure 2.

Third-Point Hoisting

The load speeds must be increased on this point and there is no need to hold the light-line speed to a low value as the two intermediate speeds provided on the first two points are sufficient. The load speed can be increased by removing the motor shunt resistor which also permits the light-line speed to increase. The circuit is shown in Figure 2.

Fourth-Point Hoisting

This point gives greater hoisting speed which is obtained by cutting the value of line resistance.

Fifth-Point Hoisting

The higher speed on this point is obtained by connecting the motor directly to the line.

First-Point Lowering

Extremely low speeds with all values of load are desirable on this point properly to land heavy loads. In addition, the motor should be capable of paying out cable with no load on the hook. The low speeds may be obtained by using a compound-wound motor, as a shunt-wound machine with heavy field excitation. This is accomplished by connecting the series field to the line through a resistor and shunting the armature across the series field. If the value of line resistance is kept low enough, the motor will have sufficient breakaway torque to start a light line downward. The speed on this point is extremely low, even with the heaviest overhauling load. The circuit is shown in Figure 2.

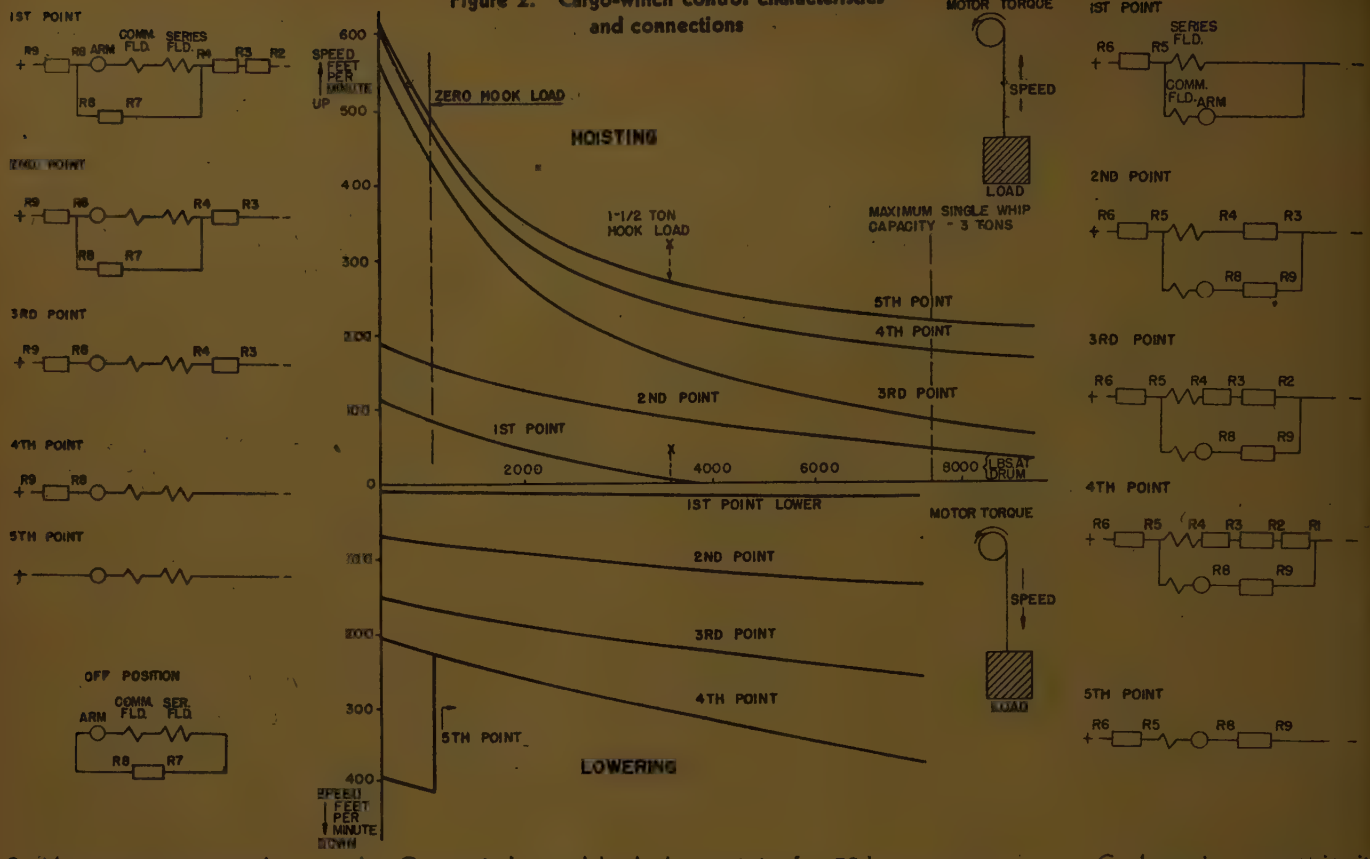
Second-Point Lowering

The higher speed that is required on this point is obtained by weakening the

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Figure 2. Cargo-winch control characteristics and connections



B. Hoisting power circuit diagrams

A. Cargo-winch speed–load characteristics for 50-horsepower motor geared to winch with 15 to 1 ratio

C. Lowering power circuit diagrams

motor-field excitation. A resistance is also inserted in the armature circuit, so there will be a little more variation in speed with load. This lines up the full-load speed on this point with the no-load speed on the third point. See Figure 2. This is important when burtoning a load, as one winch is handling full load and the other no load. The circuit is shown in Figure 2.

Third-Point Lowering

The increased speed on this point comes from a further reduction in field excitation, by the increased resistance in the series-field circuit. The rest of the circuit is the same as that on the second point. The full-load speed on this point lines up with the no-load speed on the fourth point, which is valuable for burtoning (see Appendix 1). The circuit is shown in Figure 2.

Fourth-Point Lowering

Another speed increase is obtained by a further reduction in series-field excitation. Full-load speeds on this point line up with the speed at no load on the fifth point.

Fifth-Point Lowering

A high light-line lowering speed to decrease the time to return the empty hook must be furnished on this point. This speed is obtained by reducing the field excitation to a low value.

In this case, the series field is entirely removed from the circuit, and the only field comes from the shunt field. This speed is extremely high, and it would not be safe to handle loads at this speed. For this reason, the connection is maintained only with light loads, and, if the hook is loaded, the control circuit automatically returns the motor to the fourth-point connections.

Off Position

An emergency dynamic-braking circuit, made up of the series field, the armature, and a resistor, is available in this position. This circuit automatically will provide a restraining torque for any overhauling load in case of power failure.

Description of Control-Circuit Problems

A number of interesting control-circuit problems was encountered in obtaining the desired motor power connections. Rapid response of the motor to the movement of the master-switch handle is needed to give good jogging characteristics. When the master-switch handle is moved, circuits are established to release the brake and to energize the motor. The pickup speed of the brake and the time required for the contactors to close determine the ease with which the load may be jogged. The brake pickup speed is increased by the use of a quarter-voltage coil on the brake, connected to the line through a resistor. This combination reduces the effective circuit inductance which cuts the time of the current buildup, and thereby increases the pickup of the brake. The contactors designed for this application pickup and drop out much faster than the ones previously used. These contactors are connected in such a manner that they pick up directly from the master switch and do not have

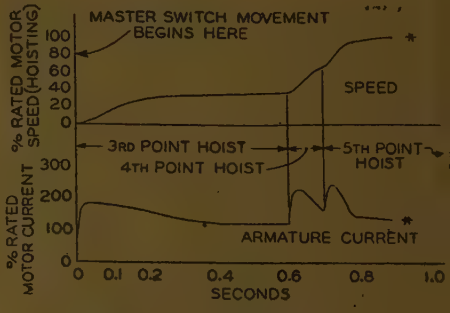


Figure 3. Acceleration curves

Hoisting from standstill to full speed with full load on the hook. Master switch thrown instantaneously from off position to fifth-point hoist

* Motor operation on fifth point is with weakened shunt field

to wait for sequencing from other devices.

Another function of the control is to make rapid acceleration of the load possible. This is important when lowering, as it permits the stevedore to swing a load under the hatch coaming by starting the load to swing and then paying out cable as it swings in the desired direction. The more rapid the acceleration, the more the advantage that may be taken of the swing to place the load in the desired position. Rapid acceleration in the hoisting as well as in the lowering direction reduces the total travel time. Very rapid load acceleration without exceeding maximum permissible torque is obtained by the proper spacing of the speed-torque characteristics in the hoisting direction, the addition of a timing relay to maintain operation on point three for a half a second, and the operation on point four for as long as it takes 3*A* contactor to pick up through an interlock on 2*A*. The current-versus-time and speed-versus-time curves for fast movement of the master switch are shown in Figure 3. Faster acceleration in the lowering directions is obtained by increasing the motor torque that drives the load downward. Gravity helps accelerate the loads in the lowering direction, and rapid release of the brake is important.

The control problem that requires more attention than all others is the establishment of a decelerating circuit that will smoothly slow down a load being lowered at high speed without causing poor commutation. The circuit must be laid out on the basis that a stevedore may be operating the winch at full load and full speed, and may then rapidly return the master switch to an intermediate or the off position. When the winch is lowering full load, the motor is acting as a generator with a very weak field and its counter electromotive force is equal to or slightly higher than line voltage. As the master switch is moved toward the off position, the resistance in the motor loop circuit is decreased and the counter electromotive

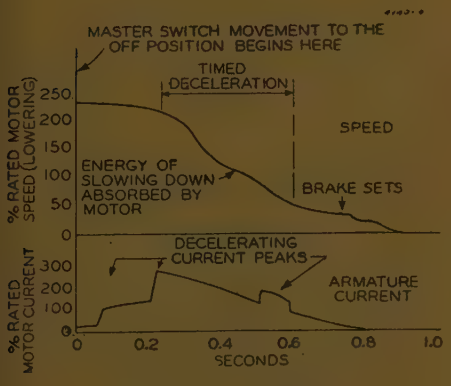
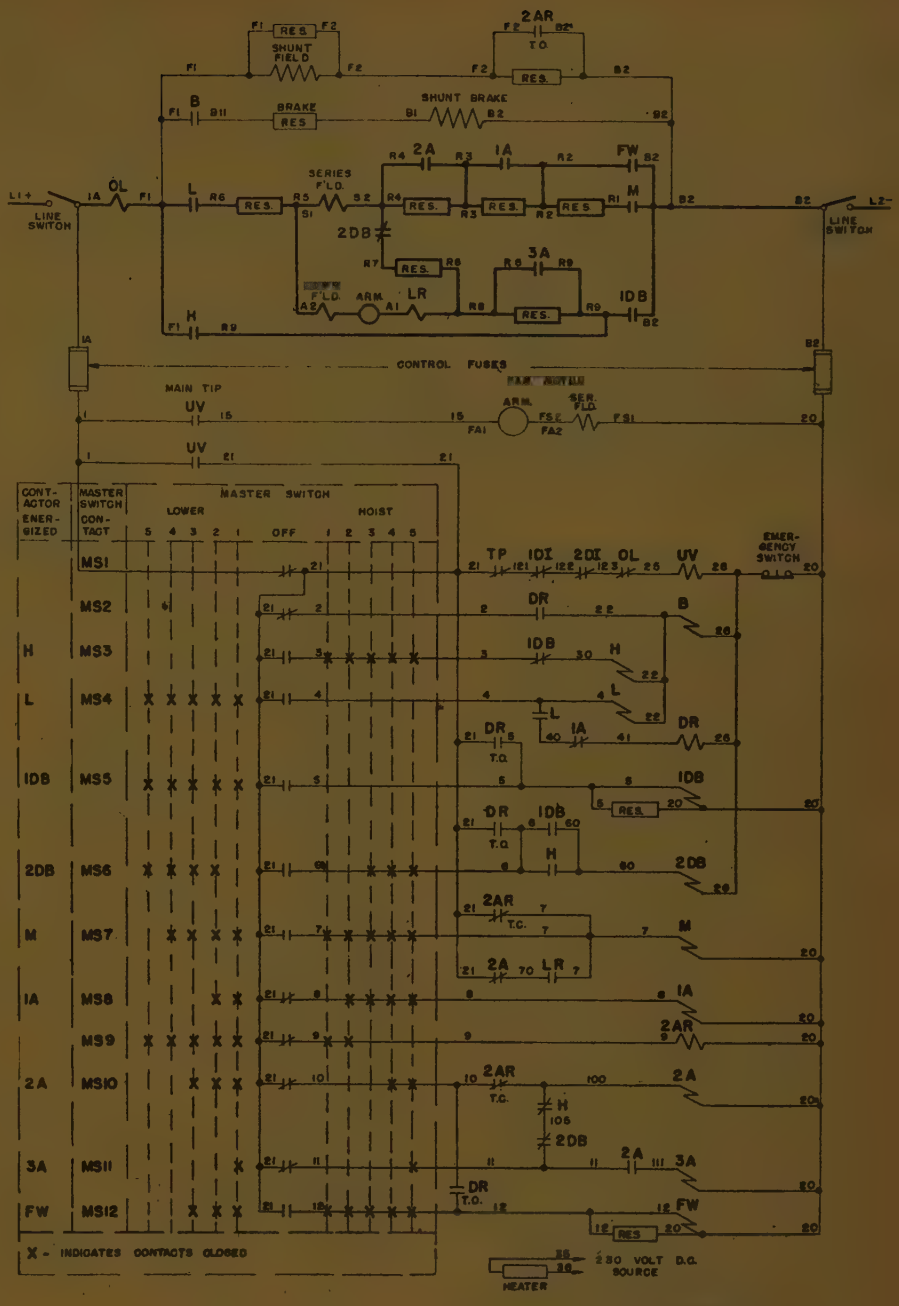


Figure 4. Test data

Deceleration from high-speed lowering with full load to standstill. Data from oscillogram taken during factory test on cargo winch



CONTACTORS

NOMENCLATURE			SYMBOLS
M	LINE		
H	HOIST	H-L, H-IDB MECHANICALLY INTERLOCKED	
L	LOWER		
IDB			
2DB	DYNAMIC BRAKING		
IA			
2A	ACCELERATING		
3A			
FW	FIELD WEAKENING		
B	BRAKE		

RELAYS

NOMENCLATURE		SETTING		SYMBOLS
UV	UNDERVOLTAGE	PICKUP- 165 VOLTS. D.C. ABOVE 65 VOLTS		SERIES COIL
DR	DECELERATING	0.5	SECONDS TO RELEASE	SHUNT COIL
2AR	ACCELERATING	0.5		NORM. OPEN CONTACT. T.O. INDICATES TIME DELAY ON OPENING
OL	OVERLOAD	550	AMPS	
LR	LOAD RELAY	27- OR BELOW	AMP REV CURRENT LOWERING	NORM. CLOSED CONTACT. T.O. INDICATES TIME DELAY ON CLOSING

DEVICE SEQUENCE CHART

DEVICE	LOWER 5	4	3	2	1	OFF	HOIST 1	2	3	4	5
UV	X	X	X	X	X	X	X	X	X	X	X
LR	X*										
H							X	X	X	X	X
L	X	X	X	X	X						
IDB	X	X	X	X	X						
2DB	X	X	X	X					X	X	X
M	X*	X	X	X	X		X	X	X	X	X
IA				X	X	X		X	X	X	X
2A					X	X				X	X
3A					X	X					X
FW		X	X	X			X	X	X	X	X
2AR	X	X	X	X	X	X	X	X			
DR	X	X	X								
B	X	X	X	X	X		X	X	X	X	X

X - INDICATES DEVICES ENERGIZED

X* = DEVICES ENERGIZED ON OVERHAULING LOAD ONLY

LIMIT SWITCHES

TP	THERMAL PROTECTOR
1DI	AIR DOOR INTERLOCKS
2DI	

Figure 5. Elementary diagram of cargo-winch control



Figure 6. Front view of unit winch control with all doors open

series field, with a light load. The use of a normally closed interlock on $2AR$ connected to the coil of M prevents removal of the series field with a light load. The use of a normally closed interlock on $2AR$ connected to the coil of M prevents removal of the series field if $2AR$ is de-energized and the shunt field weakened. The control circuit with the safety features mentioned that gave the necessary power connection is shown in the elementary diagram (Figure 5).

Construction of Control Equipment

The specifications for cargo-winch control on the newer vessels require the control equipment with the exception of the master switch to be mounted directly on the side of the winch. In this location the equipment must be protected from the action of rain and waves, as well as take a minimum amount of space. The standardized Maritime Commission design for control equipment has the electric brake, the control panel, and the resistor located in separate compartments of a large waterproof enclosure on the

force will circulate a large current through the motor armature and series field. If the loop-circuit resistance is cut out quickly before the load is slowed down and the counter electromotive force decreased, the current values will be large, and poor commutation will result. For this reason it is necessary to sequence the action of the contactors to give a time delay in reducing the resistance in the loop circuit. It is not enough to sequence the devices; a time-delay relay must provide prolonged operation with an intermediate value of resistance. This slows down the load, thereby reducing the counter electromotive force and the resulting current peak when the loop-circuit resistance is decreased. The current-versus-time and speed-versus-time curves obtained for rapid deceleration of full load are shown in Figure 4. The automatic-deceleration feature not only makes it possible to bring the load to rest as quickly as practical, but also permits plugging from the lowering to the hoisting direction. The deceleration of the load by the motor reduces the job to be done by the brake in stopping the load. On the winch-control circuit, the release of the electric brake is held off until the timed deceleration is completed. This prevents the brake from setting until the dynamic-braking circuit has reduced the speed at which the brake would be applied to approximately one half. This reduces the wear on the linings considerably.

One other part of the winch-control circuit is the safety features provided. The use of a shunt-wound brake is necessary on this application, since the current distribution between the series field and armature circuit is not well suited for the use of a series-wound brake. The series brake usually is considered very safe, as it is located in the power circuit (between $S2$ and $R4$), and a failure in the supply sets the brake. Some of the safety features of a series brake can be obtained when using a shunt brake, if the coil of the brake contactor is connected in series through H or L coils. This prevents release of the brake if coils of either of these devices fail. The most serious failure which could occur would be that of the H contactor when hoisting, as the load starts to drop, and there is not as much tendency for the stevedore to return the handle to the off position. An emergency switch is provided for stopping the motor. The circuits are set up so that failure of H or IDB contactors de-energizes the coil of the normally closed emergency contactor $2DB$. This establishes the off-position dynamic-braking loop circuit. Another safety feature is the interlocking provided between the relay $2AR$ and contactor M. The relay $2AR$ weakens the shunt field when it is de-energized, thereby providing higher light-line speeds on the last three hoisting points. If this relay should fail to remain in the closed position in the lowering direction, the motor speed would be high. This is very important on the fifth point as the contactor M is opened, removing the



Figure 7 (left). Normally open contactor

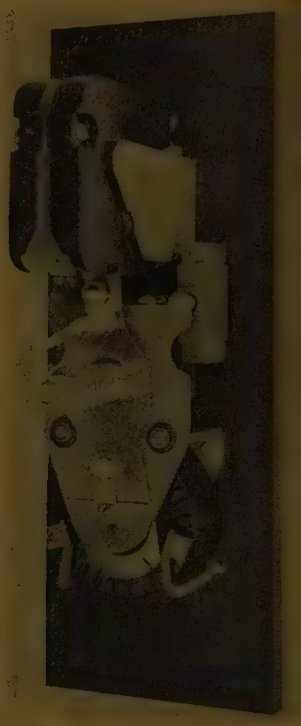


Figure 8 (right). Normally closed contactor

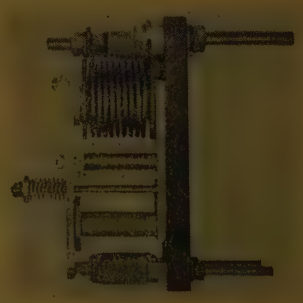


Figure 9. Load relay

side of the winch. See Figure 6. The case is constructed of $^3/_{16}$-inch steel, reinforced with partitions to give it increased strength. The control panels located in the middle compartment must be easy to service and relatively easy to remove if this should be necessary. This necessitates the panel being built in three sections which may be removed from the front of the enclosure. The weight and size of each panel is kept small by the use of a new-type marine contactor. The new normally open as well as the normally closed contactors have their armatures pivoted on synthetic-rubber bearings. The normally closed contactor is provided with an Alnico magnet to give rapid opening of the main contact tip. See Figures 7 and 8. Totally enclosed electrical interlocks are used with these new contactors. The relays are located on the bottom panels, and the load relay, which gives high-speed lowering with light load, is especially designed for this application. The relay is both small and lightweight. The device has a magnetic circuit made up of an Alnico magnet and a series coil carrying motor-armature current. See Figure 9. The design of the device is such that it gives positive snap action when the critical value is reached. A heater of 100-watt capacity is located in the panel compartment to keep the temperature above the outside ambient temperature, and prevents condensation of moisture.

The magnetic brake is solenoid-operated with the solenoid located above the motor shaft. This construction keeps the width of the brake compartment small. The brake designed for this application, shown in Figure 10, is a two-shoe brake. The brake resistor also is located in the compartment.

The power resistor (see Figure 11), which is located in the remaining compartment, is called upon to dissipate quite a bit of power, particularly on the slow-speed point. It was not possible to furnish a totally enclosed resistor in the available space because of the temperature rise of the resistor and case that would result. The only alternative was to provide forced-air ventilation for the resistor. The specifications require the resistor compartment to be watertight when the equipment is not being used and the ship is at sea. The openings used for air intake and outlet for resistor ventilation are provided with watertight doors. These doors are opened when the equipment is to be operated, and interlock switches connected to the doors prevent operation of the equipment unless the doors are open. The resistor units, boxes, and interconnections are built to operate, even though rain is being driven into the air openings. The entire front of the resistor compartment may be removed, if it is necessary to replace resistor units. All of the units are mounted in unit

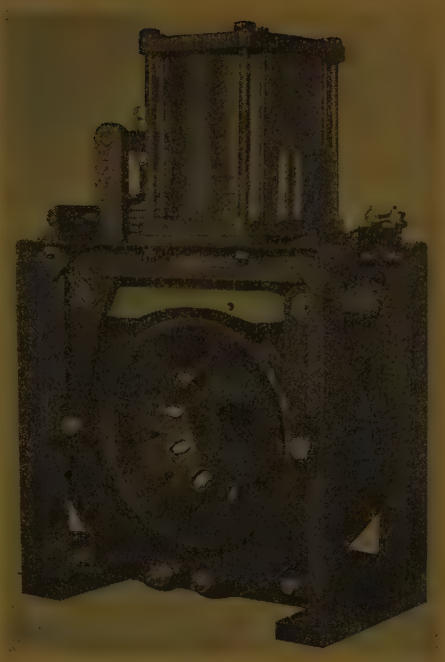


Figure 10. Magnetic brake used on unit cargo winch

boxes. There are seven of these boxes, and each box slips into its own tray and is held in place by a clip at the back and two screws at the front. The interconnections between boxes are such that they may be removed from the front and the box taken out easily. The resistor ventilation is furnished by a propeller-type fan which is capable of delivering 1,000 cubic feet of air against a static pressure of $^3/_8$ inches of water. This fan is driven by a one-quarter-horsepower, 1,750-rpm series-wound motor. The series-wound motor being used to obtain maximum air delivery is located below the resistor. The fan actually delivers 1,500 cubic feet of air a minute, as the fan load is less than fan motor rating and the motor runs at 1,965 rpm.

It was necessary to run a series of tests to determine the manner in which the resistor should be built. The first test that was run on the resistors was to determine the amount of power that could be put in each box with different numbers of boxes carrying current. That is, the top box is capable of carrying 145 per cent of its open power rating when no other boxes are in the circuit, but when all seven boxes are in the circuit the top box can only carry 105 per cent of its open power rating without exceeding the permissible temperature rise. These data were used to determine the best location for each section of resistance in the stack of boxes. After the sections were located, it was possible to estimate the length of time each section could be left in circuit for the various master-switch positions. Data regarding the time required for resistor units of this type to reach temperature with different percentages of their rated power applied made such estimating possible. On the basis of preliminary layouts, a sample resistor was made and tested under a great variety of operating conditions which would be encountered in actual service. Data already collected from numerous shipboard tests made over a period of years served as a background for determining how long the resistor should be capable of operating on each point. Assumed duty cycles are of little value in estimating the sizes of the various resistor sections because of the abuse this type of control receives from unexperienced operators. The final design of resistor contains 35 units and has ample capacity for operation on average duty cycle for 15 minutes without forced-air ventilation. Another feature of the resistor compartment is the thermal



Figure 11. Front view of resistor compartment with door removed

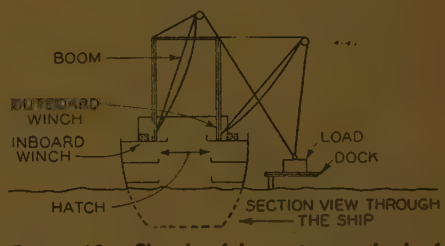


Figure 12. Sketch of burtoning method of loading and unloading a ship

switch which is employed to protect the resistor from dangerous overheating due to some unusual conditions, such as fan stoppage or blocking of the air-intake door. The fan motor is fed from the main contact tips on the control undervoltage relay and through the control fuses which should give fan operation as long as the control is energized.

The resistor is very difficult to protect, as it is essential to trip out the control for a small amount of power in the stack or from a relatively large amount of power in the same length of time. The reason for this is that the resistor is made up of six sections, used in various circuit locations, and each section has a different power rating and time on requirements. A number of different types of thermal switches was tested, and it was found that the switch had to have a best heat response to protect the edgewise resistor units which do not have a great amount of thermal capacity. The next problem was to determine the exact spot where it should be placed in order to protect as many of the sections as possible on the various operating parts. The location of the thermal switch was above the resistor stack, so that the heat representing large kilowatt dissipation low in the stack would trip the switch. The unit also would function with a relatively low kilowatt dissipation on the top units. The action in the latter case was mainly from radiation from the top units. This switch is entirely waterproof, as it is located where rain could damage it. It is not necessary to remove the thermal switch to take out resistor boxes, as it is mounted on the top of the air baffle. The thermal switch selected protected the resistor sections on most operating points as well as protecting the resistor on a normal operating cycle.

Appendix

The method of cargo handling known as burtoning consists of the use of two cables connected to the load. One cable is fed over a boom which is swung above the hatch of the vessel, while the other cable is fed over a boom above the dock. A separate cargo winch is used for each boom. Each of these winches is under the control of its own master switch. The winch that feeds cable over the boom above the dock is known as the outboard winch. The load is hoisted from the dock by the outboard winch. The winch that feeds the cable over the boom above the hatch is known as the inboard winch. The inboard winch pulls in slack cable but does not hoist any part of the load as the load is moved above the dock. As soon as the load is far enough above the dock that it may be transferred above the hatch, the inboard winch is speeded up. This pulls the load toward the hatch, and the load may be moved more by paying out cable from the outboard winch. The load may be lowered into the hatch by lowering the load on the inboard winch and paying out cable on the outboard winch at a speed slightly in excess of the inboard-winch cable. The sketch in Figure 12 indicates the relative positions of the cargo booms on the vessel. T. ere is hardly any swinging of the load with experienced operators. This method of cargo handling permits rapid loading and unloading of a vessel.

Operation of Rectifiers Under Unbalanced Conditions

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THE development of single-anode rectifying elements has greatly stimulated the application of rectifiers to general industrial and essential duty loads. Sealed ignitron rectifiers[1] have been made available in a range of ratings, and industry is vitally interested in the performance of these equipments when operated under conditions of partial-anode complement, as when an anode is misfiring or is entirely out of service.

A multiphase mercury-arc rectifier and its associated transformer are designed, so that, under normal conditions, the current in all circuit elements will be balanced, and there will be no d-c components of flux in the transformer core. When the rectifier equipment is operated on less than normal-anode complement, the circuit is no longer symmetrical, and the remaining anodes do not share the d-c load currents equally. Under this condition, a d-c component of flux exists in the transformer core, and the interphase transformer is adversely affected by unbalanced currents in the two halves.

The purpose of this paper is to describe the distribution of currents in the various transformer windings and rectifying elements under conditions of unbalance and to indicate the effect of such unbalances on the circuit duty and the rectifier capacity.[2] The paper is concerned with the temperature rise of transformers, transformer life, and the duty on the rectifying elements arising from both the magnitude and wave shape of the anode currents. The problem is best approached from an analysis of measurements made on standard equipments.

Scope of Tests

The mercury-arc rectifier utilizes a number of rectifying elements in combination with transformers and other apparatus. The heating of most of the components is determined by rms values, and the equipment is designed to meet definite temperature rise limits. The important factor in establishing temperature limits is the thermal endurance of insulation and other materials used in construction. It is well known that the physical deterioration of insulating materials under the influence of time and temperature increases rapidly with temperature. The temperature rise, therefore, influences directly the probable life and reliability of the apparatus.[3]

The design and construction of the rectifying elements are such that time and temperature have little effect on life. The capacity of the device is limited only by the ability of the ionized vapor to conduct the current from the anode to the mercury pool and by the probability of arc-back which increases as the peak current increases. The usual design of the rectifying element is, therefore, a compromise between these factors. A design for low arc-back frequency is accomplished by baffles and shields which tend to aid the deionization properties at the expense of arc-drop and a decrease in the peak ampere capacity. These factors are not directly determined by temperature rise, since the capacity of the rectifying element is influenced by the characteristics of the circuit. The accelerated-load test, or "load-limit test,"[4] is used primarily to determine the capacity of the rectifying element.

In order to determine the d-c load current which can be carried when operating on less than normal-anode complement without undue heating of any part of the rectifier transformer, or exceeding the maximum loading at which the rectifying element is capable of operating without failure, it is necessary to know the distribution and wave shapes of current in the primary, secondary, and interphase windings. The procedure used in obtaining data of this nature is to make temperature measurements and to study wave shapes of currents with typical circuit arrangements, so that the analysis of

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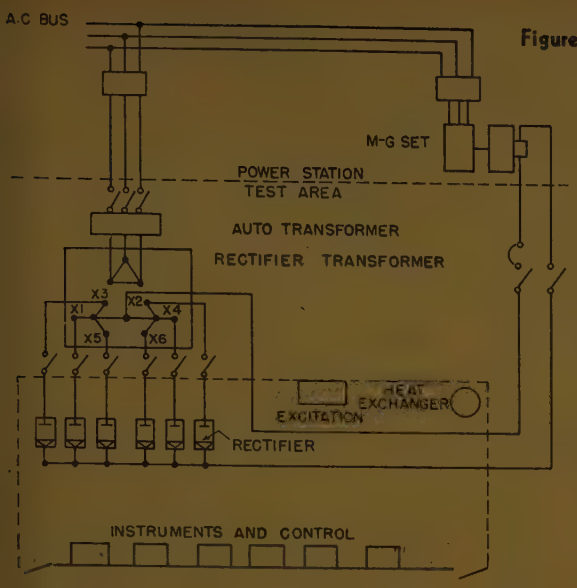


Figure 1 (left). Arrangement of test equipment

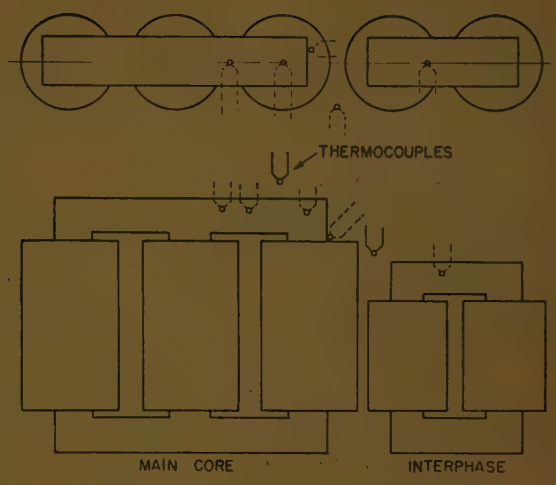


Figure 2 (right). Thermocouple locations

Double-wye transformer

data obtained in a few specific cases will enable the design engineer to predict the performance for a range of ratings.

A typical circuit diagram of equipment for determining rectifier-circuit duty operating with less than normal-anode complement is shown in Figure 1. Shunts and ammeters connected in the anode leads permit the reading of the average anode-current distribution. A multi-element oscillograph is used to obtain the wave shapes of all anode and transformer primary currents.

For most applications, circuits which utilize simple rectifiers operating in parallel are preferable. In such circuits the total load is divided equally between the simple rectifiers. Arc losses are a minimum, and the current capacity of the equipment is a maximum. Tests, therefore, were conducted on equipments with double-wye transformers rated 150 kw and 1,000 kw at 250 volts direct current. Tests also were made on a 200-kw 275-volt d-c double-biphase transformer. Test results on the 150-kw and 1,000-kw equipment were very similar; hence, discussion is limited to the 1,000-kw unit.

To determine the transformer heating, readings of the top oil, ambient, and core temperatures were taken under various conditions of loading. For core-temperature measurements, thermocouples were placed in the main and interphase core during assembly with leads brought out through a manhole opening in the cover. The location of the thermocouples for the double-wye transformer is shown in Figure 2. Because of the difficulty in locating the thermocouples on the hottest surface, which is the underside of the top yoke, they were placed between laminations near the top and measurements corrected for the hottest surface temperature.

To determine the duty on the rectifying elements, as well as to evaluate factors affecting the permissible transformer load, an analysis was made of the primary line current, anode currents, and voltage oscillograms.

Test Procedure

Normal Operation

To provide a basis of comparison, a normal heat run was made on a rectifier equipment with all anodes conducting. The rectifier transformer was rated 1,190 kva and 6,600/243 *L-N* volts with a double-wye secondary winding designed for a 1,000-kw 250-volt 6-anode ignitron rectifier. The primary voltage was maintained at 6,600 volts, 60 cycles, and the load was adjusted to a 4,000-ampere d-c output, at zero phase retard, and continued until constant oil temperature rise was obtained.

Since the a-c voltage across the interphase transformer and, consequently, its core losses increase with the amount of phase retard, the run was continued with 15 per cent phase retard, until constant interphase core rise over oil was reached. Oscillograms were taken of the primary

Figure 3. Core and top-oil temperature rise

1,000-kw double-wye transformer, five-anode operation

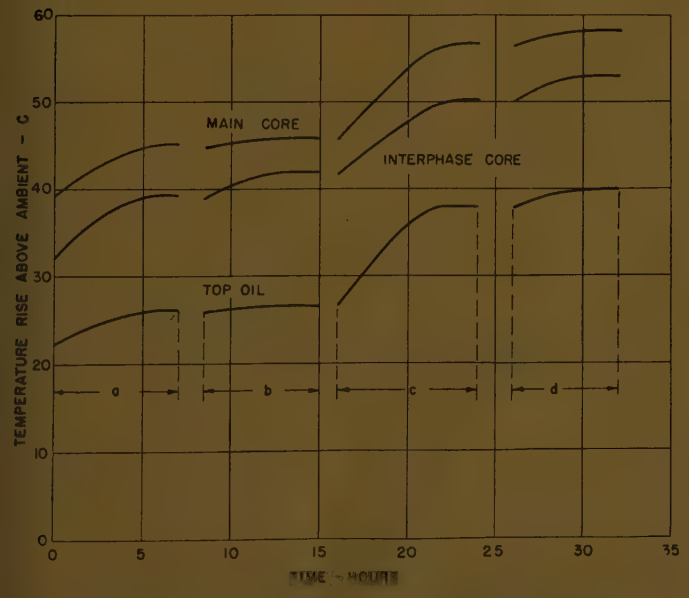
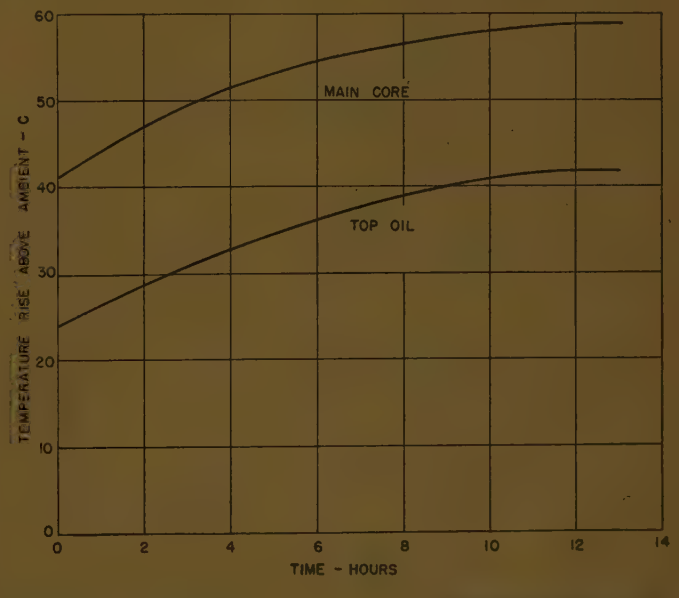


Figure 4. Core and top-oil temperature rise

1,000-kw double-wye transformer, three-anode operation



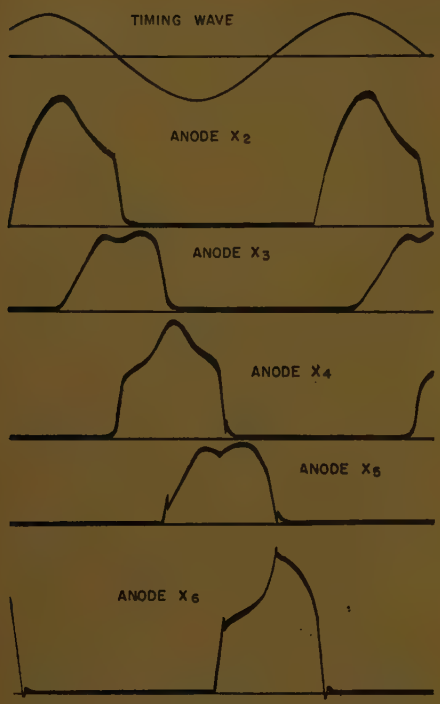


Figure 5. Anode-current wave forms, double-wye rectifier, anode X_1 inactive

voltage, primary and secondary currents in all phases.

Five-Anode Operation

Temperature measurements were made on the rectifier transformer operating on five anodes with anode X_1 disconnected. The primary voltage was maintained at 6,600 volts, 60 cycles. The load was reduced to 2,300 amperes d-c output at zero phase retard and held until constant main and interphase core rise over oil was reached, as shown in Figure 3*a*. The phase retard then was adjusted to 15 per cent, and the run continued, as shown in Figure 3*b*.

The load was increased then to five sixths of full-load rating or 3,333 amperes at zero phase retard and maintained until constant temperatures were reached, as shown in Figure 3*c*. The phase retard was set at 15 per cent, and the load held until constant core rise over oil was obtained, as indicated in Figure 3*d*.

Three-Anode Operation

To obtain data with one wye inactive, another run was made with the rectifier operating on anodes X_2, X_4, and X_6. The primary voltage was maintained at 6,600 volts, 60 cycles. It was recognized that more than one-half load could be carried with one wye operating; therefore, two-thirds rated load or 2,670 amperes d-c output at 15 per cent phase retard was held until constant oil rise was obtained. The results of the test are shown in Figure 4 and Table I.

Table I. Temperature Rise—Degrees Centigrade at 15 Per Cent Phase Retard

Double-Wye Transformer

Per Cent Load	Number of Anodes	Top Oil Above Ambient Temperature	Main Core Above Oil	Interphase Core Above Oil
100	6	41.5	17.5	17
57.7	5	26.5	19.0	15
83.3	5	40.0	18.0	13
66.7	3	42.0	17.5	0

Double-Biphase Rectifier

Similar measurements were made on a 200-kw 275-volt double-biphase rectifier. A heat run was made operating with all anodes conducting at 15 per cent phase retard and 100 per cent load current. The test was continued when operating with three anodes at 15 per cent phase retard and 75 per cent load current.

Analysis of Data

Temperature Measurements

A summary of temperature measurements of Figures 3 and 4 are shown in Table I.

It may be seen that the transformer core temperature rise over oil does not increase appreciably when operating a rectifier on less than normal-anode complement, and that, under any of these test conditions, the hottest core surface rise above ambient temperature did not reach the permissible limit of 65 degrees centigrade. The core temperature, therefore, is not a determining factor in limiting the load which can be carried under this condition of operation.

Table II

Anode	2	3	4	5	6
Peak amperes	3,159	1,810	2,610	1,948	3,087
Average amperes	831	427	644	526	832
Rms amperes	1,466	789	1,138	926	1,462
Ratio effective/average	1.76	1.85	1.77	1.76	1.76

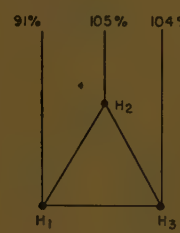
Table III

Harmonic	Per Cent I_L
Fundamental	89.0
2	45.0
3	
4	6.3
5	6.8

Table II shows the data obtained when an analysis is made on a typical oscillogram of anode current, as shown in Figure 5.

The rms current distribution in per cent of rated current in the various transformer windings of a double-wye rectifier with one anode inactive and with zero phase retard is shown in Figure 6. It may be noted that the output of the rectifier will have to be reduced approximately to 60 per cent of rated d-c current in order not to exceed the normal current rating of any winding. Under emergency conditions, however, it may be necessary to overload and, consequently, decrease the transformer life expectancy.

The current distribution for a double-wye rectifier operating with three anodes in one wye inactive is shown in Figure 7. It may be noted that, at 66.7 per cent rated load, the primary current is 102



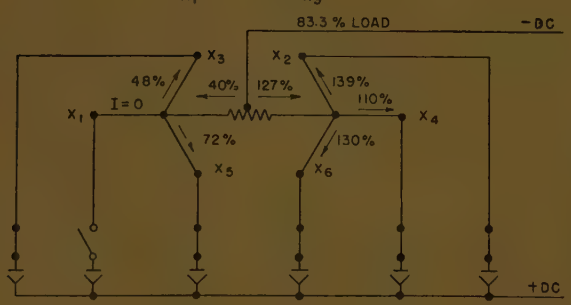
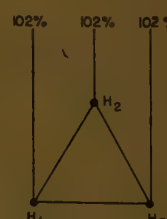


Figure 6 (left). Rms current distribution in per cent rated current

Double-wye rectifier, five-anode operation



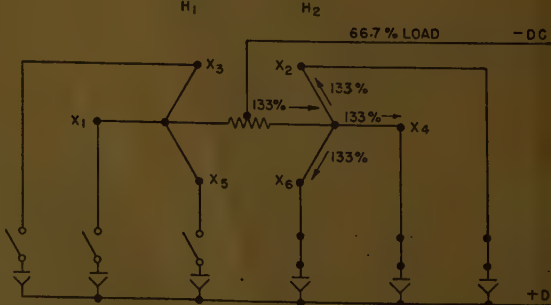


Figure 7 (right). Rms current distribution in per cent rated current

Double-wye rectifier, three-anode operation

Table IV. Temperature Rise—Degrees Centigrade

Per Cent Load	Number of Anodes	Oil Above Ambient Temperature	Main Core Above Oil	Teaser Core Above Oil	Interphase Core Above Oil
100	4	28	4	4	8
75	3	31	7	7	11

per cent of normal rated load which indicates a large component of exciting current and considerable d-c flux in the transformer core. Another disadvantage of operating with one wye is that the distortion of primary line current is increased. The analysis of an oscillogram of primary line current gives the harmonic components in per cent of the total effective current, as shown in Table III.

The temperature rises obtained when one anode is inactive in a double-biphase rectifier operating at 15 per cent retard are summarized in Table IV.

The rms current distribution in the various elements for 75 per cent rated load current is shown in Figure 8. The output will have to be reduced approximately to 50 per cent of full load in order not to exceed the rated current of any transformer winding.

Duty on Rectifying Elements

The duty on the rectifying elements when operating on less than normal-anode complement arises from both the magnitude and the shape of the current and voltage waves. Since the instantaneous arc-drop is a function of the instantaneous current, the peak current is an important factor in determining the duty on the rectifying element. On the other hand, the residual ionization is a function of the peak current which has passed during conduction and, therefore, influences the probability of arc-back. The ratio of peak current to load current in the anode following an inactive anode in a double-wye rectifier increases from 0.5 for normal operation to 1.0 under operation with one anode inactive. A similar increase is noted for the anode preceding an inactive anode.

The final commutation rate provides a further measure of the duty on the rectifying element. The residual ionization present at the beginning of the inverse period depends on the rate of reduction of current during the last few degrees of conduction. Kingdon and Lawton[5] have indicated that the frequency of arc-back is a function of the product of initial inverse voltage by the final commutation rate. Both of these factors are increased when one anode in a rectifier is inactive; however, if the loading is reduced to come within the current limitations of the transformer windings, the frequency of arc-back is not increased. Figure 9 shows load-limit tests on a double-wye rectifier operating with normal-anode complement compared with five-anode operation. The five-anode load limits are roughly 70 per cent of the limits with all anodes active.

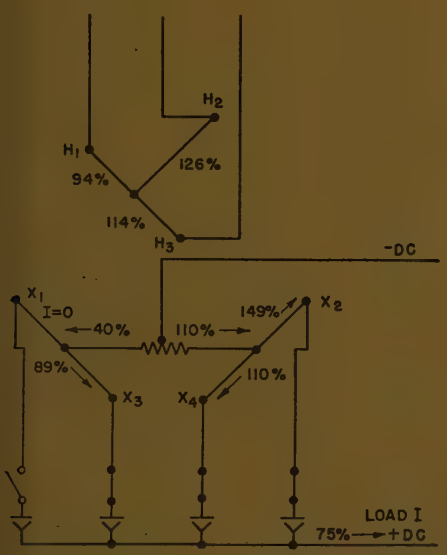


Figure 8. Rms current distribution in per cent rated current

Double-biphase rectifier, three-anode operation

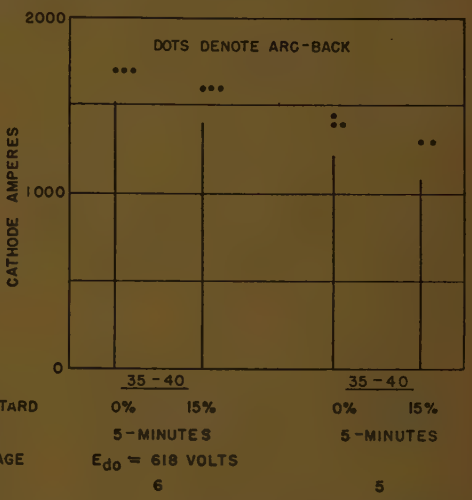


Figure 9. Load-limit tests

Double-wye rectifier, normal- and partial-anode complement

Conclusions

1. The transformer core temperature increases somewhat when operating a rectifier on less than normal-anode complement, but it is not a factor limiting the amount of load which can be carried under this operating condition.

2. Because of the large unbalances in rms coil currents with one anode inactive, it will be necessary to reduce the load to considerably less than five-sixths load for the double-wye connection and three-fourths load for the double-biphase connection. The tests indicate that the load will need to be reduced to 60 per cent for the double-wye connection and to 50 per cent for the double-biphase circuit, if the current rating of any transformer winding is not to be exceeded.

3. A very large exciting current is present when a double-wye transformer is operated with only one wye carrying load, indicating the presence of considerable d-c flux in the core.

4. If the anode of a double-wye rectifier is inactive, more load usually can be carried on the remaining five anodes than can be carried using one secondary wye.

5. If one anode of a double-biphase-connected rectifier is inactive, as much load can be carried on two anodes operating single biphase as can be carried on the three remaining anodes.

6. When the loading is reduced, because of the current limitations imposed by the transformer windings, the arc-back frequency is not increased when operating a double-wye rectifier on less than normal-anode complement.

References

1. Sealed-Tube Ignitron Rectifiers, M. M. Morack, H. C. Steiner. AIEE Transactions, volume 61, 1942, August section, pages 594–9.

2. Rectifier-Circuit Duty, C. C. Herskind. AIEE Transactions, volume 63, 1944, March section, pages 123–8.

3. Loading Transformers by Temperature, V. M. Montsinger. AIEE Transactions, volume 49, 1930, page 776.

4. The Testing of Mercury-Arc Rectifiers, H. L. Kellogg, C. C. Herskind. AIEE Transactions, volume 62, 1943, December section, pages 765–73.

5. The Relations of Residual Ionization to Arc-Back in Thyratrons, K. H. Kingdon, E. J. Lawton. *General Electric Review*, volume 42, November 1939.

An Aircraft Differential-Voltage Cutout

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Synopsis: Experience obtained with present multiengine aircraft electrical systems indicates a need for preventing interchange of current between generators when the system is operating at light loads. Such current interchange causes reverse-current cutouts of conventional design to connect and disconnect their respective generators to the system so rapidly that the action is called "chatter." Conventional cutouts connect the generator to the system when the generator voltage is above a pre-set value. The differential-voltage reverse-current cutout connects the generator to the system when the generator voltage is a pre-set value above the system voltage. A description is given on the design, operation, and advantages of a differential-voltage reverse-current cutout which prevents interchange of current between the generators.

ONE of the major problems on large multiengine aircraft electric systems is the prevention of the interchange of current between the generators when the system is operating at light loads. Parallel operation of the generators is accomplished with a circuit using equalizer windings on the voltage regulators. The effectiveness of this circuit is proportional to the difference in load current of the generators. This means that there is no paralleling action at no load and very little paralleling action at light load, which is the normal condition on an airplane. Loads nearing the capacity of the system may be encountered for only a few minutes on each flight. Voltage regulators have an accuracy of plus or minus two per cent over the entire range of ambient temperatures and generator speeds. The nominal setting for voltage regulators is now 28 volts. The specified tolerance allows the voltage regulators to regulate the generator voltage at any point between 27.5 volts and 28.5 volts. This small variation in voltage causes reverse current to flow to one or more generators. Reverse current flowing to a generator causes the reverse-current cutout to disconnect the generator from the system. However, the generator voltage may be above the pickup setting of the cutout so that it will reconnect the generator to the system.

Consider the case of an aircraft electrical system containing two generators operating in parallel under the control of two voltage regulators and two conventional cutouts.[1,2] The voltage regulator of the first generator is set at 28.5 volts and its cutout is set to connect the generator to the system at 26.5 volts. The voltage regulator of the second generator is set at 27.5 volts and its cutout is also set to connect the generator to the system at 26.5 volts. With both generators connected to the system, current will flow from the first generator to the second generator. This condition causes the cutout of the second generator



Figure 1. A 200-ampere differential-voltage cutout

to disconnect it from the system. With the cutout of the second generator opened by reverse current, the voltage on its operating coil is 27.5 volts; which is one volt higher than its operating voltage. Therefore, the cutout of the second generator will reconnect the generator to the system and the action just explained repeats. Due to the rapidity of this action, it is called "chatter." When this condition occurs the respective cutouts are continually operating, shortening their life in some instances to relatively few hours.

From this description it is evident that there is a need for a method of preventing interchange of current between generators when the system is operating at light loads.

The simplest means of eliminating the cutout "chatter" is to change the principle of operation from a device which measures the generator voltage (automotive type) to a device which measures the difference between the generator voltage and the system voltage. Hence, this new cutout is called a differential-voltage cutout. The differential-voltage cutout operates when the generator voltage exceeds the system voltage by a predetermined difference voltage. The system voltage may vary over a wide range without "chatter" of the differential-voltage cutout. In electrical systems using the differential-voltage cutout a generator cannot be connected to the system until the system voltage decreases to a point where the generator has sufficient voltage to supply part of the load.

Description

Figure 1 shows a differential-voltage reverse-current cutout designed for use with any d-c generator having a normal regulated voltage of 28.5 volts and a current rating up to 200 amperes. The device occupies a four-inch cube and weighs two pounds six ounces. Four mounting holes on three-inch centers are provided in lugs extending from the cover. All terminals extend from the top so that cables may be attached from any direction. The unit is entirely enclosed for operation in atmospheres of the severest dust and moisture content. Its operation shows negligible change when vibrated at frequencies up to 55 cycles per second with a total excursion of $^1/_{16}$ inch.

The differential-voltage cutout connects the generator to the system when the generator voltage is 0.4 volt above system voltage. The system voltage may be at any voltage above 20 volts. A reverse current of 16 amperes causes the cutout to disconnect its generator from the system. Besides the main function, the cutout performs other necessary duties:

1. Operates on a dead bus—when a generator is up to speed and there is no other source of voltage on the system this cutout connects the generator to the system when a load as small as eight watts is connected.

2. Operates as a starting switch—when the differential-voltage cutout is installed with an auxiliary power plant its contactor coil may be energized separately from the bus so that the contactor serves as a starting switch to start the auxiliary engine.

3. Protects the system against reversed generator polarity—if the generator builds up with reversed polarity, it is possible to have 60 volts across the differential circuit of the cutout. Under this condition the cutout does not operate nor is the differential circuit damaged even though its operating voltage is only 0.67 per cent of this value.

4. Protects the system against short circuits—should a generator suddenly lose its field or become short-circuited, a heavy surge of reverse current will flow through it.[3] The cutout must be capable of opening

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this current without damage to the cutout or further damage to the generator. The contactor of this cutout has interrupted 3,500 amperes at 50,000 feet altitude.

Design

A differential-voltage cutout presents several design problems not encountered with the conventional cutout. The primary considerations are protecting the differential-voltage winding against overheating and interrupting the differential-voltage winding to prevent discharging the battery when the generator is idle.

Because the differential-voltage winding must have sufficient excitation to operate on an extremely low voltage, it is impractical to make it withstand full system voltage (for the case of a dead generator) or double system voltage (for the case of a reversed generator) without overheating. The winding is protected by connecting a relay across the differential circuit which operates at a low enough voltage to protect the differential-voltage winding by inserting a resistor in the circuit.

On the conventional cutout the operating coil is connected to the generator side of the contactor so that the coils are not energized when the generator is idle. The nature of the differential-voltage cutout makes it necessary to interrupt the differential-voltage circuit, in addition to the main circuit, to prevent discharging the battery when the generator is idle.

Two main units comprise the cutout illustrated by Figures 1 and 2: a polarized differential-voltage reverse-current relay, distinguished by the square coil, and a magnetic contactor. Two auxiliary relays control the differential-voltage winding on the reverse-current relay. Figure 3 is a schematic diagram of the cutout.

The square spool of the reverse-current relay coil contains three windings. An inner differential-voltage winding *RCD* is connected between the generator positive terminal and the battery positive terminal through the contacts of the two auxiliary relays. A holding winding *RCH*, in series with a 470-ohm resistor, is connected between the reverse-current relay contacts and ground. An outer series winding *RCS*, consisting of a single turn of copper strap, is connected between the generator positive terminal and the generator side of the contactor. The differential-voltage winding operates the reverse-current relay contacts *RC*. The holding winding produces the magnetic force to hold the reverse-current relay contacts *RC* closed when the differential-voltage winding is short-circuited by the contactor contacts *C*. The series winding adds magnetically to the differential-voltage and holding windings when the generator is supplying current to the system. Polarization is accomplished with a permanent magnet.

The design of this relay produces a strong opening force on the relay armature when a heavy surge of reverse current, such as can be caused by a short-circuited generator or the loss of a generator field, flows through the series winding. As the reverse current increases, the relay opening force increases.[1] It is impossible for this type of relay to "lock in" on a heavy reverse current.

On other types of reverse-current relays it is possible for a heavy surge of reverse current in the series winding to reverse completely the polarity of the magnetic circuit. When this condition occurs, the relay holds its contacts closed and the heavy reverse current continues to flow, causing additional damage to the system.

A special design is used on the operating magnet of the contactor. Magnetically it operates as a solenoid magnet in

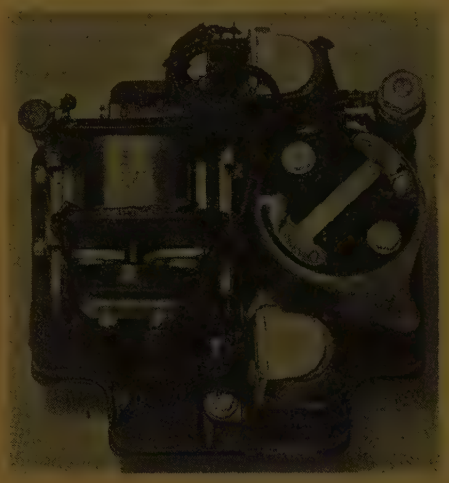


Figure 2. A 200-ampere differential-voltage cutout with cover removed

the open position and as a lifting magnet in the closed position. This arrangement uses advantages of each type of magnet and produces a contactor of minimum weight for the forces obtained. The contactor coil is composed of a main winding *CM*, and a holding winding *CH*. The main winding has low resistance and is used to operate the contactor. When the reverse-current relay contacts *RC* close, full generator voltage is impressed across this winding. As the contactor contacts *C* close an electrical interlock opens, making the holding winding *CH* in series with the main winding. The contact force obtained with this contactor is 4.5 pounds per contact, producing a voltage drop across the cutout of only 60 millivolts at 200 amperes.

Both auxiliary relays have two contacts in parallel for added reliability in completing the low differential-voltage circuit. Relay *A*1 is normally open and is in series with a rectifier. It is used to complete the differential-voltage circuit when the generator voltage is over 20 volts. The rectifier is used as a d-c valve and prevents the closing of relay *A*1, if the generator builds up with reversed polarity. Relay *A*2 is normally closed and is connected across the differential-voltage winding. It protects the differential-voltage winding against over voltage. When the voltage on the differential-voltage winding exceeds four volts a resistance of 80 ohms is inserted in the circuit.

Operation

With switch *S* in the number 1 position the differential-voltage cutout is ready for operation. When the generator speed is increased until its terminal voltage is 20 volts, relay *A*1 will operate and complete the differential-voltage circuit. With a battery voltage of 25 volts there is 5 volts across the differential winding. Since this voltage is also applied to the coil of *A*2, the latter operates and inserts the resistor in series with the differential-voltage winding *RCD*. Increasing the generator voltage decreases the differential voltage. When the generator voltage is equal to the battery voltage there is no voltage on the coil of relay *A*2. Therefore, *A*2 releases and closes its contacts, causing the resistor to be short-circuited. Now the circuit has only the low resistance differential-voltage winding to limit the current in the differential-voltage circuit. Increasing the generator voltage to 0.4 volt above the battery voltage causes contacts *RC* to close, energizing the contactor coil *CM* and the reverse-current relay coil *RCH*. The contactor coil closes contacts *C* which connects the generator to the load bus. The differential-voltage cutout is now in the normal operating position. Decreasing the generator voltage below the battery voltage causes reverse current to flow from the battery to the generator. With current flowing in the reverse direction, relay coil *RCS* magnetically opposes relay coil *RCH*. Increasing the reverse current to 16 amperes causes the reverse-current relay to open the contactor coil circuit and disconnect the generator from the load bus.

If the generator builds up with reversed

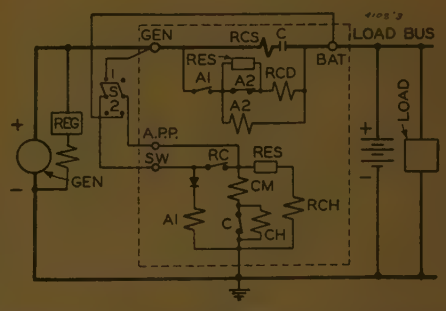


Figure 3. Schematic diagram of the differential-voltage cutout

polarity the rectifier will block the current and prevent the closing of relay $A1$.

To use the contactor of the differential-voltage cutout as a starting switch for starting an auxiliary engine, place switch S in the number 2 position during the starting period.

Advantages

Two fundamental improvements are obtained in the operation of aircraft electrical systems by using the differential-voltage cutout in place of the conventional cutout:

1. Cutout "chatter," which occurs with conventional cutouts when the system is operating at light loads, is eliminated.

2. The generator may be connected to the system at any voltage above 20 volts. This is particularly important on long flights where the generator speed is below the speed necessary to produce normal voltage. With this condition the battery supplies all of the load current until its voltage drops below the generator voltage. At this point the generator is connected to the system, preventing complete discharge of the battery.

Three additional improvements are included in the differential-voltage cutout which produces greater reliability of operation than is obtained with the conventional cutout:

1. The contactor of the cutout protects the system when a short circuit occurs in the generator.

2. Operation is not impaired in atmospheres of the severest dust and moisture content.

3. Operation is not impaired by vibration conditions encountered in aircraft service.

Conclusions

It has been shown that a cutout which is designed for differential-voltage operation has all of the advantages and none of the disadvantages of the automotive type of cutout. Also, the differential-voltage cutout has several advantages not obtained by using any other principle of operation.

The outstanding success of the differential-voltage cutout is exemplified by the preparation of an Army-Navy Specification for a differential-voltage cutout intended to supersede the automotive type of cutout.

References

1. Aircraft Voltage Regulator and Cutout, R. C. Jones, D. W. Exner, S. H. Wright. AIEE Transactions, volume 61, 1942, June section, pages 334–9.

2. The Application of Voltage Regulators to Aircraft Generators, L. W. Thompson, F. E. Crever. AIEE Transactions, volume 61, 1942, pages 363–5.

3. Present D-C Aircraft Electric-Supply Systems, H. C. Anderson, S. B. Crary, N. R. Schultz. AIEE Transactions, volume 63, 1944, June section, pages 265–72.

A Unique Moving-Magnet Ratio Instrument

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Synopsis: This paper describes a unique type of moving-magnet d-c ratio instrument. An instrument designed for temperature measurement in aircraft is described in detail.

THE remote indication of oil and coolant temperatures on aircraft has been accomplished principally through the use of temperature-sensitive resistance bulbs and moving-coil ratio instruments. A survey of the requirements indicated that a moving-magnet instrument could be developed which would fulfill these requirements and have characteristics of simplicity, serviceability, ruggedness, and lightness. Such instruments, both single and dual, are illustrated in Figure 1.

Principle of Operation

In this instrument the permanent-magnet rotor position and the indication are determined by the fluxes produced by two mutually perpendicular coil groups. One coil group carries substantially constant current; the other carries current which is a function of the temperature bulb resistance.

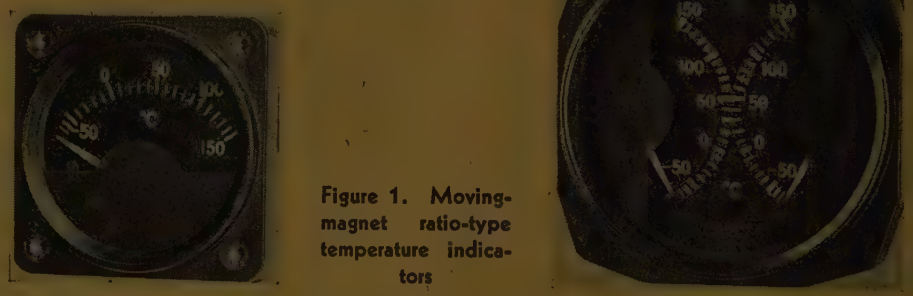


Figure 1. Moving-magnet ratio-type temperature indicators

Figure 2 shows the coils and rotor arrangement. The small outer coils serve to set up a diametrical flux along the axis of these coils. This flux corresponds to vector B in Figure 3. The current in these coils is approximately constant for all indications at any given supply voltage.

The large inner coils serve to set up a diametrical flux along the axis of these coils and perpendicular to the axis of the small outer coils. This flux corresponds to vector A in Figure 3. The current in these coils is a function of the quantity to be indicated. These coils serve as the detector in a Wheatstone bridge in which case the coil current is zero near center scale, a positive maximum at one end of the scale, and a negative maximum at the other. This is illustrated in Figure 3 where vector A is downward at −70-degrees-centigrade indication, zero at +40-degrees-centigrade indication, and upward at +150-degrees-centigrade indication. In this diagram vector C is the resultant flux. The rotor is magnetized parallel to the flat surfaces, and the rotor aligns itself with the resultant flux lines. Figure 3 is helpful for purposes of an elementary explanation of operation but does not include various distorting effects.

In the actual device the two sets of coils are surrounded by a high-permeability cylindrical shield. The purpose of this shield is to provide a return path for the internal fluxes and to screen out extraneous fields.

The Circuit

The circuit used is shown in Figure 4. Resistors R_1, R_2, and R_3 are fixed arms in a Wheatstone bridge, and R_4 is the resistance-type thermometer bulb. All resistance values are practically independent of temperature except those of the coils and bulb. Bulb-resistance characteristics are shown in Figure 5. The bridge circuit is balanced by the bulb at approximately center-scale (+40 degrees centigrade) indication and is unbalanced at all other indications. At indications below approximately 40 degrees centigrade unbalance current flows through coil A from R_2 to R_4. At indications above this value current flows through coil A in the opposite direction. Because of the values of fixed resistances in the circuit, variations in bulb

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resistance cause less than ±5 per cent variation in the outer-coil current for all scale indications at any given values of voltage and ambient temperature. For elementary considerations this current may be assumed constant.

Construction and Adjustments

Figure 6 illustrates the construction of a single-element temperature indicator. The element is cut away showing the close magnetic coupling between the rotor and the two inner coils. These coils are wound on insulated copper-coil forms, which provide a damping structure close to the rotor to damp excessive pointer oscillations.

The permanent magnet rotor material is sintered oxide, chosen for its high coercive force and low density. The dimensions of the rotor were selected as a result of tests made to determine the optimum allocation of space to coils and rotor, for the case of a 0.7-inch-diameter enclosing shield. The rotor is mounted on an aluminum shaft provided with tantalum–tungsten pivots. The pointer is secured to the shaft over locating flats by a nut.

Directly above the element are two adjustable resistor spools, each consisting of a fixed-coil portion and a small rheostat.

Resistance R_3 in Figure 4 includes one of these adjustable rheostats. This controls the balance point of the bridge and, consequently, substantially fixes the location of the center-scale point.

Resistance R_7 is similarly adjustable by the other rheostat and by shunting action determines the amount of current in coil B, hence, the length of vector B in Figure 3, and consequently the scale length. Permanency of these adjustments is accomplished by soldering the rheostat contact arms to the windings.

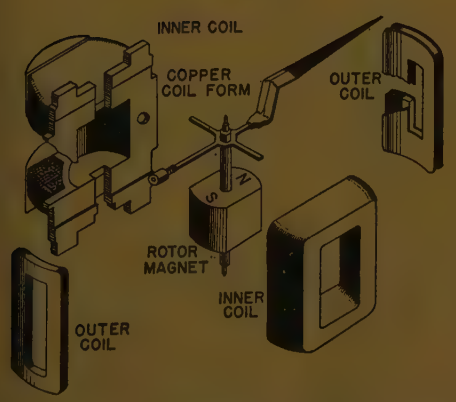


Figure 2. Arrangement of coils and rotor

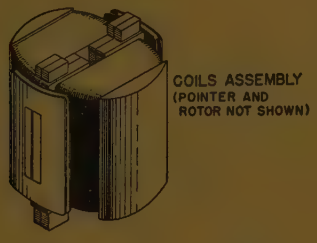
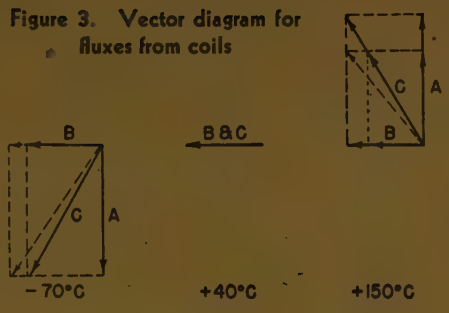


Figure 3. Vector diagram for fluxes from coils



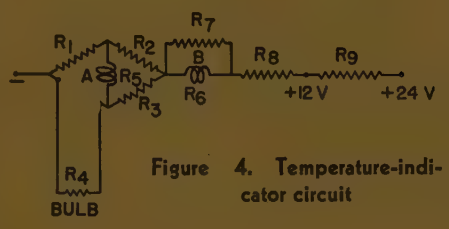


Figure 4. Temperature-indicator circuit

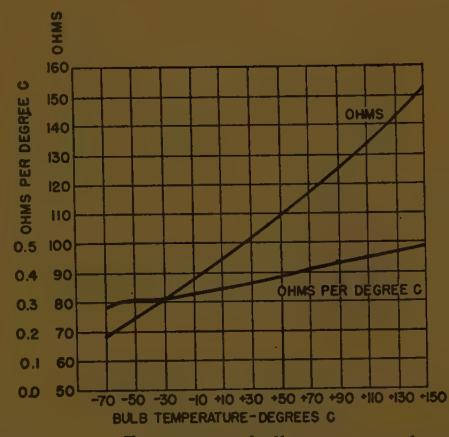


Figure 5. Temperature–bulb-resistance characteristics

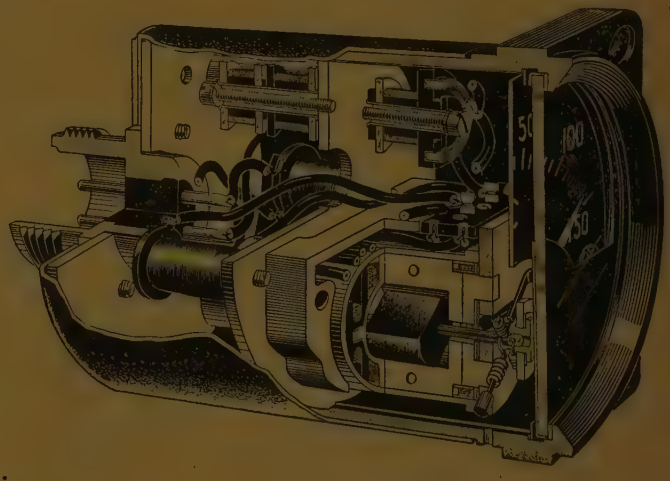


Figure 6. Sectional view of single-element moving-magnet ratio-type temperature indicator

Scale Distribution

In order to attain the required linear scale the introduction of nonlinear effects is necessary. Figure 7 shows the crowding at the scale ends, which would occur if the effective vector B of the small outer coils were constant for every scale position, and if the vector A of the inner coils were of a magnitude proportional to the variation of the indicated quantity from the center-scale value. Figure 8 shows how a gradual shortening of vector B, with departure from center scale, makes the scale distribution uniform.

The nonuniform distribution of Figure 7 is approached when a purely cylindrical rotor is used. The uniform distribution of Figure 8 is practically realized if the rotor has flat sides parallel to the rotor-flux lines.

Figures 9 and 10 illustrate the manner in which the flat-sided rotor effects a linear-scale distribution.

The flat-sided rotor produces a large reduction in magnetic coupling between the outer coils and the rotor with rotation away from the center-scale position. This is illustrated by Figures 9 and 10. This effect corresponds to the desired shortening of vector B.

Power-Failure Indication

Power-failure indication is accomplished by a small magnet assembled in the subbridge with the magnetic axis perpendicular to the center-scale rotor axis, so that, in the absence of excitation, the pointer is moved off the lower-scale end. The torque of this small magnet is supplemented by a permeance torque obtained by shaping elliptically the shield enclosing the coil structure, and by aligning the minor axis properly.

Both torques vary in an essentially sinusoidal manner with the permeance torque in second harmonic relationship to that of the small magnet. These torques and their uniform resultant are shown in Figure 11.

The uniform resultant provides adequate torque well beyond the scale ends and eliminates the peak at center scale, thus minimizing the frictional and voltage-error problems.

The down-scale shift in indication, which these torques tend to produce, is corrected by adjustment of R_3, Figure 4.

Voltage Error

Fundamentally, the rotor position depends upon the ratio of the currents in the

two sets of coils and not upon magnitudes. In the absence of pull-off magnet and shield torques, the indication would be independent of applied voltage.

The pull-off magnet and shield torques cause the pointer to move down scale as the supply voltage and, consequently, the torque contributed by the coils are reduced. Reduction of voltage from 28.5 to 22.5 volts generally causes errors less than three degrees centigrade at any scale point.

Ambient-Temperature Compensation

Errors caused by change in ambient temperature are of two types. The first is a change in scale length which is caused by disproportionate changes in the currents in coils A and B. With reference to the right-hand diagram in Figure 3 and the circuit of Figure 4, suppose that the ambient temperature is raised, and suppose that R_7 is omitted. Then, because of the increased resistance of the copper coil A, the current in that coil would decrease, and vector A would be shortened. However, the current in coil B would not change appreciably, and vector B would not change. The dashed vector C shows the resultant in this case. The angle between the solid vector C and the dashed vector C represents a shortening of the scale.

If resistance R_7 of low-temperature-resistance coefficient is placed in the circuit, then the current in the copper coil B can be made to vary in accordance with the variation of the current in coil A. At room temperature a portion of the battery current passes through coil B and is governed by R_6 and R_7. At elevated temperatures a lesser portion of the battery current passes through coil B. With reference to Figure 3 again it is apparent that, if vectors A and B are shortened proportionately, the resultant vector falls on the room-temperature resultant, no change in scale length occurs, and compensation is accomplished. Since this type of compensation depends upon diverting a fraction of the battery current through coil B, and since the ampere turns produced must be correct for a given scale length, it follows that this type of compensation is primarily a function of the number of turns in coil B.

The second type of temperature error is caused by a change in torque as the resistance of coils A and B vary. Suppose that at elevated temperature both coil currents decrease by say ten per cent. Then the torque contributed by the coils also will decrease ten per cent. The pull-off torque then will cause the pointer to move in a down-scale direction for all indications. This effect is compensated by including in bridge arm R_2 a certain amount of positive-temperature-coefficient material, in this case, copper. If the ambient temperature is elevated R_2 increases, and the bridge values change in a sense to cause the pointer to move up scale. Since bridge arm R_2 is opposite to bulb arm R_4, an increase in R_2 acts in the same direction as an increase in bulb resistance.

Self-heating errors are negligible because of the very low heat dissipation and the corrective effects of the ambient-temperature compensations.

Table I includes observed ambient-temperature errors on a typical instrument at −50- and +75-degree-centigrade ambient temperatures.

Compensation attainable is better than required. It is thus possible to use R_7 for both compensating and scale-length-adjusting purposes with satisfactory results.

Factor of Merit

On an equivalent D'Arsonval basis the torque-to-weight ratio of the temperature indicator averages unity with variations of ±20 per cent.

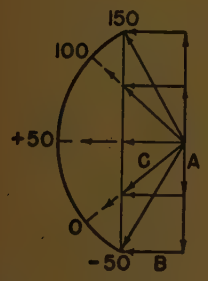


Figure 7. Scale distribution with cylindrical rotor

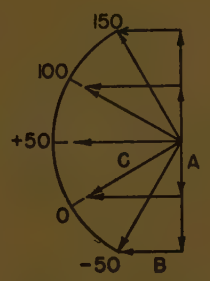


Figure 8. Scale distribution with flatted rotor



Figure 9. Coupling between rotor and outer coils at center scale



Figure 10. Coupling between rotor and outer coils 90 degrees from center scale

Table I. Observed Ambient-Temperature Errors

Indication—Deg C	Observed Temperature Error—Deg C	
	At −50	At +75
− 70	0.0	−1.0
− 30	−0.5	−0.5
0	0.0	−0.5
+ 30	0.0	−0.5
+ 80	−0.5	+1.0
+120	+1.0	+1.0
+150	−1.0	+1.0

Conclusions

The instrument described has the properties of lightweight, ruggedness, simplicity, and serviceability required for aircraft use. In addition the factor of merit is good, the voltage errors are low, self-heating errors are negligible, and ambient-temperature errors are practically compensated.

The instrument may be adapted to many applications where the characteristics of a ratio instrument are desirable.

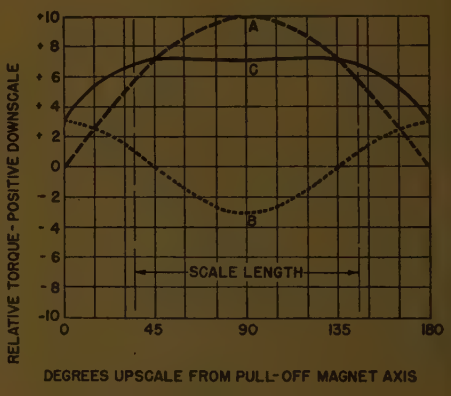


Figure 11

A. Torque curves of pull-off magnet
B. Shield
C. Resultant

Inferred uses include the measurement of ohms, current ratios, voltage ratios, telemetering, and voltage-weighted current indications.

References

1. Mechanical Factor of Merit With Respect to Electrical Instruments, J. H. Goss. *General Electric Review*, April 1933.

2. A D-C Telemeter or D-C Selsyn for Aircraft, R. G. Jewell, H. T. Faus. AIEE Transactions, volume 61, 1942, June section, pages 314–17.

3. A New Moving-Magnet Instrument for Direct Current, H. T. Faus, J. R. Macintyre. AIEE Transactions, volume 61, 1942, August section, pages 586–8.

Protection of Large D-C Machines by Means of High-Speed Circuit Breakers

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THE problems of protecting large installations of rotating d-c machines have been neglected to some extent by the lack of suitable high-speed switchgear rugged enough to stand the severe duty usually encountered. The authors are describing their analysis of probable short-circuit characteristics of such large systems and a high-speed breaker rated at 7,500 amperes which has been developed for this particular purpose. The authors are also describing a large rotary-converter installation built some time ago which has recently been modernized by means of modern switchgear in order to overcome the serious damage caused to the rotating machines resulting from the inadequacy of the originally installed switchgear.

Comparison of Short-Circuit Conditions (A-C and D-C)

There has been some confusion in regard to application of ultrahigh-speed breakers. Perhaps the curves shown in Figure 1 can help to clear up this confusion.

Curve 1 is a fully offset 60-cycle current wave. The current dies down after a few cycles and becomes symmetrical, and an attempt to interrupt it at the first one-quarter-cycle peak would merely impose a severe duty on the breaker contacts as they would operate when the maximum current is flowing. The peak electromagnetic forces in the installation will not be cut down by the one-quarter-cycle opening of the breaker. From the standpoint of breaker performance it would be desirable to delay its opening as it then will have to interrupt lower currents. A compromise must be made between system stability and breaker performance and standard a-c breakers usually open in three to five or even eight cycles. Unless system stability is of prime importance there is no material gain in paying the higher cost of high-speed breakers.

Curve 2 is a d-c curve illustrating the condition existing in a system where the d-c generators are located an appreciable distance apart. The rate of rise is very low due to the inductance, and the ultimate current is low due to the ohmic resistance of the system. Except for preventing damage caused by commutator flashovers no gain is made by the use of high-speed breakers and commercial breakers with opening speeds of $1^3/_4$ to three cycles can therefore satisfactorily be employed.

Curve 3 shows the d-c rate of rise of a 300-volt 3,000-kw generator under short-circuit conditions. The initial rate of rise is 4,500,000 amperes per second and the ultimate current is 450,000 amperes. A three-cycle breaker would have to interrupt 200,000 amperes and a $1^3/_4$-cycle breaker would have to interrupt 125,000 amperes. A $^3/_8$-cycle breaker (time to peak) would have to interrupt only 23,000 amperes. Here the value of the high-speed breaker is clearly indicated. The electromechanical stresses in the machine and the connecting bus-bar structure or conductor is only 0.77 per cent when using a high-speed breaker. The various manufacturers of d-c machines do not seem to agree on the initial rate of rise and the ultimate current on a short-circuit condition, and a discussion of this problem would be of interest, but from test data which we have on a 650-volt 6,000-kw machine we have established beyond a doubt that the initial rate of rise is more than 6,000,000 amperes per second. This curve is shown as curve 4.

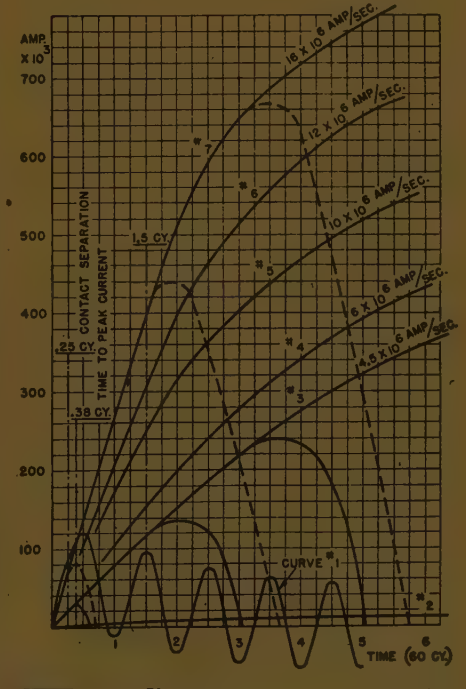


Figure 1. Short-circuit curves comparing effect of opening speed of circuit breakers

As more machines are added to the system, curves 5, 6, and 7 probably apply. If we use a three-cycle breaker in a d-c system represented by curve 7 we will have to interrupt the fantastic current of 700,000 amperes while a 0.38-cycle breaker (time to peak current) would have to interrupt only 80,000 amperes. In this case the stresses are again only 0.77 per cent if a high-speed breaker is used.

Assume a bus of 100-foot length used in this latter system with the busses placed two feet apart and consisting of two six-inch-square tubes, the force per running foot will be 110,000 pounds, requiring insulators spaced 0.064 feet apart if a three-cycle breaker is used. This of course is impractical. If we use a 0.38-cycle breaker (time to peak circuit) the force is only 1,450 pounds per running foot and the insulator spacing of 4.8 feet can be safely employed. This latter scheme would appear to be more reasonable.

It is appreciated that a bus-to-bus fault in a modern installation is rather uncommon but they do occur with disastrous results and it is emphasized that sound engineering practice dictates the installation of modern high-speed switchgear as the one solution to the problems which are encountered by high current and the electromagnetic forces they set up in the system.

Oscillographic Comparison of Current Against Time

Figure 2 shows oscillogram of an *FB* breaker interrupting a short circuit on two 3,000-kw generators at 550 volts, in parallel. The mechanical time of the breaker is 0.212 cycle, time to peak is 0.403 cycle, total time 82 cycles except for the small amount of trailing current which is permitted to flow through the arc interrupter resistors in order to keep the breaker voltage within reasonable limits. The maximum current is 26,600 amperes and the initial rate of rise is 4,920,000 amperes per second. The breaker was tripped by the reverse current feature, which trips the breaker in the shortest time possible.

The oscillogram in Figure 3 shows the breaker interrupting the same circuit but tripped by the forward-current overload feature. Due to the inherent characteristics of the overcurrent feature which employs an open air gap with a moving armature, the breaker now trips somewhat slower. The mechanical time of the breaker is 0.5 cycle, time to peak current is 0.667 cycle, and the total time is 1.17 cycles. The maximum current is 36,200 amperes or 9,600 amperes more than the current is in Figure 2 because

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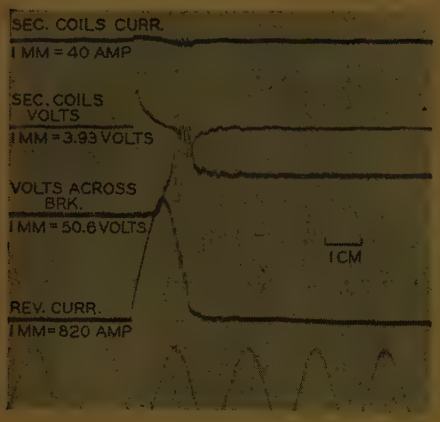


Figure 2. Oscillogram of *FB* breaker interrupting 550-volt d-c reverse-current trip

the mechanical time of the breaker is 0.288 cycle slower.

Figure 4 shows the same breaker opening the same circuit but at 650 volts and tripped by the forward current trip. The mechanical time is again 0.5 cycle, peak 0.645 cycle, total time 1.16 cycles, and maximum current 47,000 amperes, an increase of 8,700 amperes by increasing the generator voltage by 100 volts over Figure 3.

Description of Type-FB Breaker

A schematic cross section of the *FB* breaker is shown in Figure 5. The current path through the breaker is from the upper terminal through the magnetic blowout coil to the upper contact. From here it flows through the bridges to the lower contact where the circuit divides into two parallel circuits which then meet in the lower terminal.

There are two blowout coils, one located on each side of the arc chute and connected in parallel. Each coil consists of one turn and the coils are physically located so that the core of the coil is in line with the region in which the arc is originated. The return path for the blowout flux is through a laminated-iron circuit. The laminations extend through the core of the blowout coil to the outside face of the arc chute so there is no time delay in building up the blowout flux due to eddy current. Two slotted pole faces extend from the extremity of the laminated core to distribute the flux for proper arc interruption.

The movable bridge contacts are supported in an extremely light carrier made of heat-treated aluminum and the movable arcing contacts are in turn mounted on this carrier. The necessary contact pressure between the stationary and moving contacts is furnished by a completely independent separate spring on each contact located between the contact and the contact carrier.

The carrier is biased toward the open position by means of two opening springs, one located inside the other. The stronger of the two opening springs has a limited stroke and acts on the contact carrier only during the initial acceleration period. The weaker of the two springs is active during the whole opening stroke and definitely holds the contact carrier in its open position. The kinetic energy stored up in the moving contact

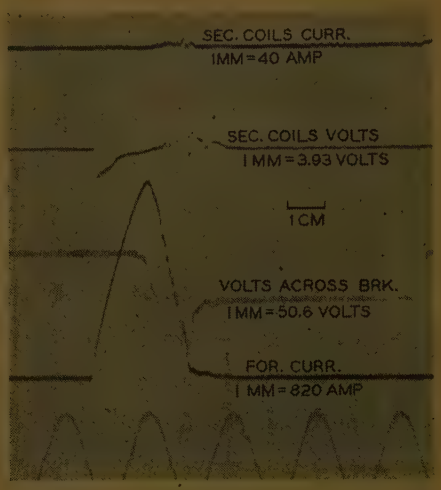


Figure 3. Oscillogram of *FB* breaker interrupting 550-volt d-c forward-current trip

during the opening stroke is absorbed at the end of the stroke by means of a friction-type buffer, preventing rebound of the contacts.

The contact carrier is held in the closed position by means of a clutch shown in the released position on Figure 6. This clutch consists of two hardened steel jaws. The two jaws contact a hardened steel roller on opposite sides. The opening force of the breaker which is 5,000 pounds tends to force the two jaws apart with a force of 300 pounds, due to their angle of contact with the roller. This 300-pound force is counteracted by means of a holding magnet of the bucking-bar type. The magnet has been described in detail.[1]

If the magnetic pull of the holding magnet is temporarily lost for any reason, the two clutch jaws will be forced apart and the breaker contacts moved to the open position by means of the two opening springs.

There are several ways the holding force may be lost:

1. By a bucking bar which will momentarily demagnetize the armature on reversal of current.

2. By a trip coil acting as a bucking bar and connected across the interpole windings of the d-c machines.[2]

3. By a mechanical prying mechanism which exercises a prying force greater than that of the magnetic holding force tending to keep the breaker closed. This mechanical mechanism can be operated by (*a*) a hand trip lever, (*b*) a shunt trip magnet, (*c*) an overload magnet located around the inductive leg of the trip circuit, or (*d*) an overload magnet located around the lower terminal.

As already explained the circuit from the lower contact to the lower terminal is divided in two paths. The upper path passes through the holding magnet and the lower path is surrounded by a stack of iron laminations. With a steady current flowing through the breaker the current will divide between the two paths proportional to the conductances of the two paths. This is true whether the current flows in a forward or reverse direction. With a rapidly changing current flowing through the breaker, the current will divide in the two paths inversely proportional to the inductances of the two paths. This is also true whether the current is flowing forward or in a reverse direction.

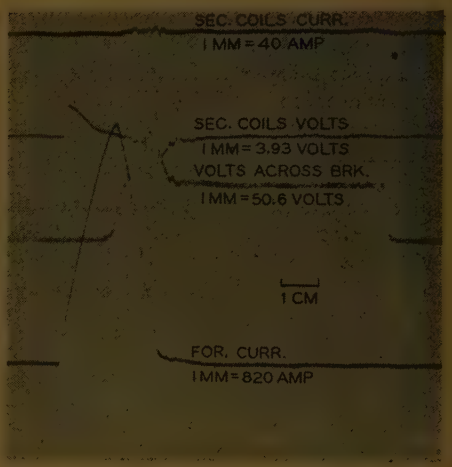


Figure 4. Oscillogram of *FB* breaker interrupting 650-volt d-c forward-current trip

On a quickly rising reverse current, the portion of the current which flows through the holding-magnet circuit will cause the holding flux through the armature to be diverted to a magnetic bypass. The armature will momentarily lose its pull and the breaker will trip for this reason. On slowly rising reverse current

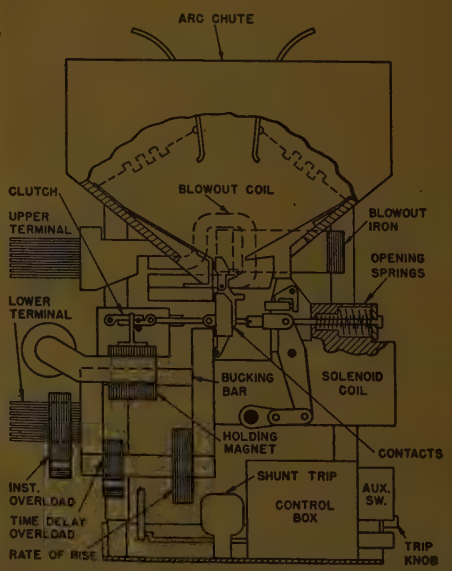


Figure 5. Section of type-*FB* breaker

the same effect will be obtained but at a higher current value.

If we place an overload magnet on the inductive part of the circuit, the overload magnet can be arranged to trip the breaker at a maintained current of sufficient value. A quickly rising current will pass harmlessly through the upper portion of the circuit due to the inductance of the lower circuit. This effect will afford overload protection of the machine and will permit harmless surges to pass through the breaker without tripping it. In other words, a time-delay overcurrent protection is provided without any mechanical timing means.

An overcurrent feature placed on the lower terminal leads where all the current which flows through the breaker will pass through this feature can be arranged to trip the breaker on excessively high current thereby tripping the breaker if its associated machine should contribute a high value of current into a fault on the system.

The operating mechanism consists of a solenoid which is arranged to close the breaker through an overcenter toggle mechanism. The opening springs and the contact buffer are built as an integral part of the closing mechanism. Therefore, all mechanical stresses in the breaker (except the contact pressures) are located in one common assembly and are not transmitted through any insulating material.

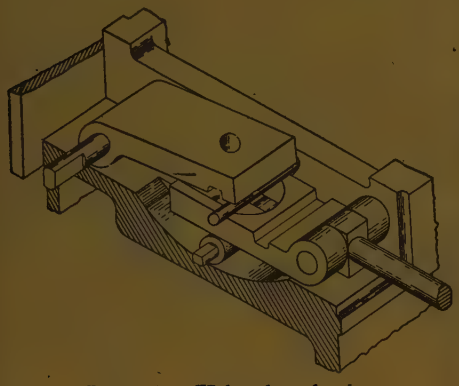


Figure 6. *FB* breaker clutch
Released position

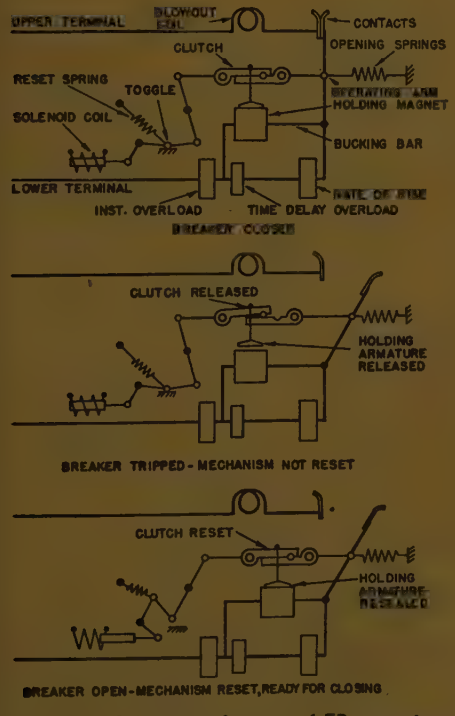


Figure 7. Schematic diagram of *FB* operating mechanism

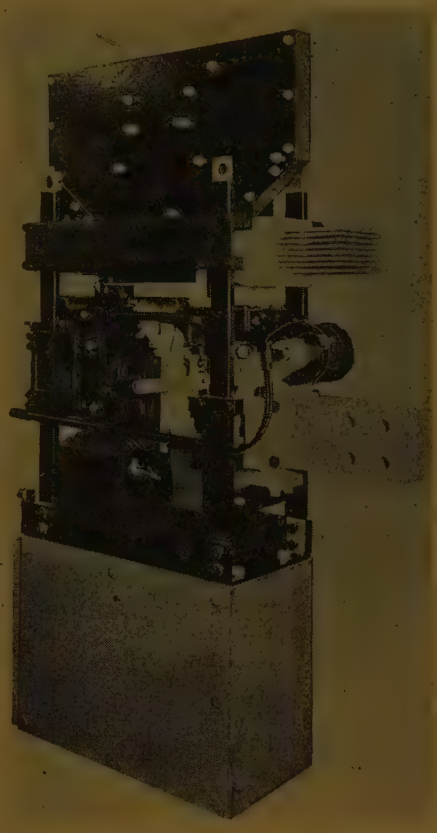


Figure 8. Right-side view of *FB* breaker

The various operating positions of the breaker are shown diagrammatically in Figure 7.

The top illustration shows the breaker closed. The opening forces in the breaker are here counteracted by means of the operating toggle which is locked over center.

The center illustration shows the breaker in the tripped position. The clutch is shown in its released position and the contact has been moved to the open position by means of the opening springs. The solenoid mechanism has not yet reset.

The bottom illustration shows the mechanism reset and ready for closing. This resetting was accomplished by means of the resetting spring causing the toggles to fold up thereby resetting the solenoid to its open position which in turn has permitted the two clutch jaws to engage the clutch roller.

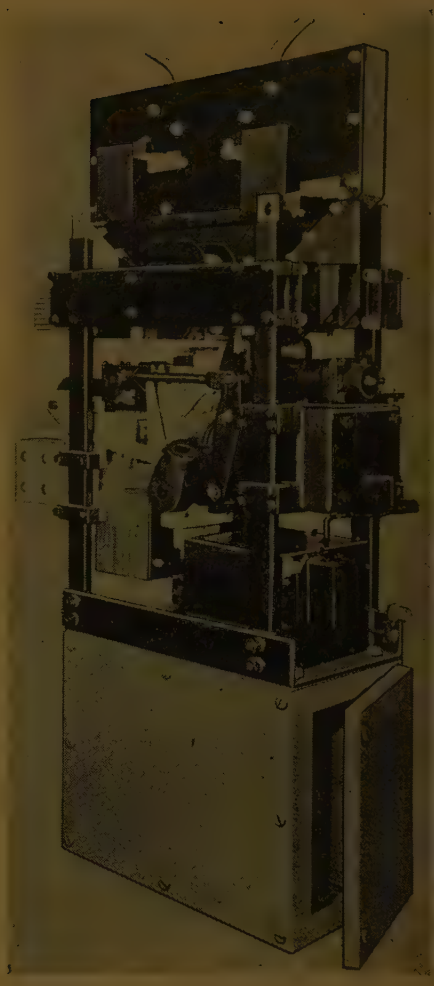


Figure 9. Left-side view of *FB* breaker

The arrangement provides trip-free operation at any part of the closing stroke. Hand operation is provided by means of a removable handle which can be inserted in a socket on one of the toggle links.

Two partial side views of the breaker are shown in Figures 8 and 9.

Description of Rotary-Converter Station

It will be of interest to describe a large d-c installation where the *FB* breakers are being installed to provide better protection of the rotating machines than was originally furnished for this installation. This plant has an installation of a large number of 2,500-kw 60-cycle rotary converters housed in two conversion stations. Only one station is being modernized and this station is described in detail.

The rotary stations are fed by a large hydroelectric power system with interconnections to other power systems. Fault capacities are very high in the a-c system, 1,500,000 kva on the 13.2-kv bus.

The station has double 13.2-kv a-c busses. The double-bus arrangement permits feeding all of the stations from one bus or splitting the load between the two busses. A one-line diagram of the

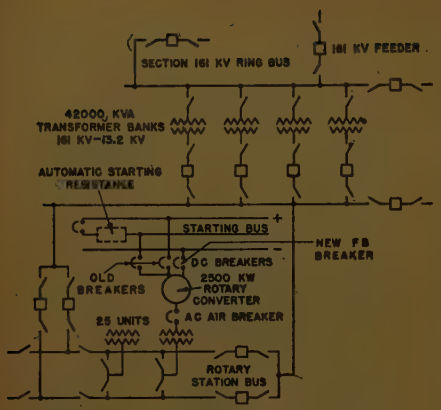


Figure 10. One-line diagram of rotary-converter station and bus

These units were considered very large when installed in 1914 and were quite sensitive to disturbances, particularly those involving rapid changes in frequency. During such disturbances the units tend to pull out of step with each other and flash over. Such troubles often result in damage to the unit, particularly the main and equalizer busses, brush rigging, and commutators. There is also occasional damage to the armature winding. Flashover may also be started by trouble on an individual unit or on the d-c busses. Most of the flashovers are on the d-c side, although there are occasionally a-c flashovers. Most of the damage during flashovers is from energy from the d-c bus.

2. Overload trip on the inductive leg, affording machine overload protection and permitting harmless surges to pass through the breaker.

3. Instantaneous high overload protection which will operate the breaker should any rotary converter contribute a larger amount of forward current into a commutator flashover on any of the machines connected to the bus.

4. Shunt trip.

5. Hand trip.

It is felt that this advancement in the state of the art of circuit-breaker design will materially reduce damage in this station during faults and that the time of

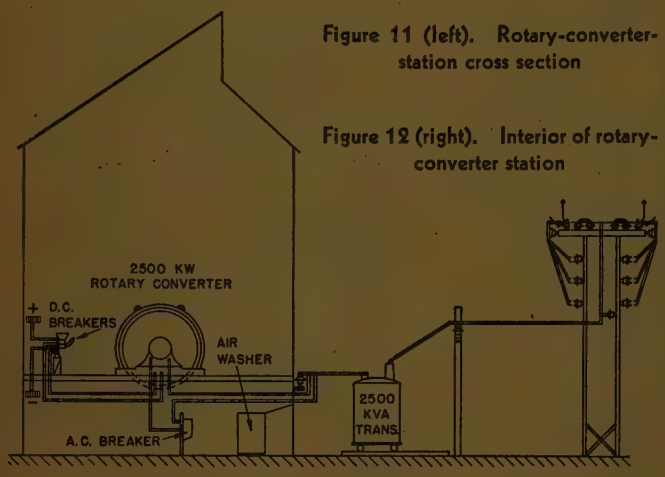


Figure 11 (left). Rotary-converter-station cross section



Figure 12 (right). Interior of rotary-converter station

busses and station is shown in Figure 10. Double disconnecting switches are provided for each unit, permitting the units to be connected to either of the busses. These disconnecting switches will break transformer magnetizing current. The transformers were originally connected to the busses through oil circuit breakers. These have been removed and replaced with low-voltage air circuit breakers of 150,000 amperes interrupting capacity between the transformer secondary and the rotary converter.

Each unit has a positive, negative, and starting air circuit breaker on the d-c terminals as shown in Figure 13. These breakers were originally conventional air circuit breakers, opening in approximately eight cycles. The rotary converters are started from the d-c side by reduced-voltage starting. One unit is provided with an a-c motor for starting in case of complete station outage and there is a similar equipment in the other station to provide alternate means of starting. Several machines, and at times all machines, are operated in parallel on the d-c busses. The physical arrangement of the station is shown in Figure 11. Figure 12 shows the interior of the station.

Analysis of the troubles indicates that the damage to units and the transmission of trouble from one unit to others could be minimized if units in trouble could be disconnected fast enough from the bus. It is felt that the speed of the d-c breakers must be particularly high to be effective in this regard. This high speed will reduce the time of heavy power inflow to the flashover and clear before the inflow can reach its peak, thereby limiting damage and the extension of the disturbance to other units. With the widespread use of mercury-arc rectifiers in the conversion field, new high-speed switchgear of improved design over that previously available was developed. It was decided that the d-c breakers would be replaced with a modern high-speed breaker of the *FB* type.

The breakers in this installation employ the following tripping schemes:

1. Reverse-current protection by means of the bucking bar releasing the holding armature. As it is necessary to start the machines from the d-c bus, a slowly rising reverse current of approximately 1,600 amperes is permitted to pass safely through the breaker but a higher reverse current or a fast-rising reverse current of a lower value will trip the breaker.

outage of load may be materially reduced.

References

1. A Fast Circuit Breaker, **D. I. Bohn, Otto Jensen.** AIEE Transactions, volume 61, 1942, March section, pages 165–8.

2. D-C Machine Flashover and Bus Short-Circuit Protection, **T. B. Montgomery, J. F. Sellers.** AIEE Transactions, volume 60, 1941, pages 1168–73.

3. Protection From Flashing for D-C Apparatus, **J. J. Linebaugh, J. L. Burnham.** AIEE Transactions, volume 37, 1918, pages 1341–52.

4. Short-Circuit Protection for D-C Substations, **J. J. Linebaugh.** AIEE Transactions, volume 39, 1920, pages 617–29.

5. Flashing of 60-Cycle Synchronous Converters and Some Suggested Remedies, **Marvin W. Smith.** AIEE Transactions, volume 39, 1920, pages 631–58.

6. The High-Speed Circuit Breaker in Railway Feeder Networks, **J. W. McNairy.** AIEE Transactions, volume 45, 1926, pages 962–9.

7. High-Speed Circuit Breakers for Railway Electrification From the Design Point of View, **H. M. Wilcox.** AIEE Transactions, volume 47, 1928, pages 1285–92.

8. A New High-Speed Air Circuit Breaker, **A. O. Keep.** AIEE Transactions, volume 53, 1934, February section, pages 522–6.

9. A New Multiple High-Speed Air Circuit Breaker for Mercury-Arc-Rectifier Anode Circuits and Its Relation to the Arc-Back Problem, **J. W. Seaman, L. W. Morton.** AIEE Transactions, volume 61, 1942, November section, pages 788–96.

Transient Performance of Induction Motors

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IT has been the usual practice to solve induction-motor transient problems either on the basis of the familiar steady-state equivalent circuit or by calculations which require simplifying assumptions. These methods neglect important factors determining the performance. This paper shows the necessity of considering for many practical problems the electrical and mechanical transient phenomena simultaneously.

For example, during sequential operations such as jogging and plugging, conditions inside the rotor, phase relations, and magnitudes of fluxes and current are influenced both by the electrical transients and by the mechanical slip and oscillation. Also, the residual stator and rotor currents and the angular displacement of the rotor affect performance during plugging; these have been neglected previously.

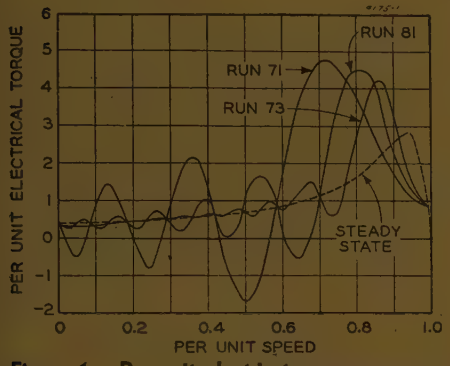


Figure 1. Per unit electric torque versus per unit speed

Effect of rotor deceleration. Transient speed–torque curves with constant rotor deceleration from steady-state conditions at 0.99 per unit speed
Run 71— 6 cycles for unit speed change
Run 81—12 cycles for unit speed change
Run 73—24 cycles for unit speed change

In order to evaluate some of these mutually interrelated phenomena which had not been analyzed previously, the induction-motor equations including the rotor mechanical load and inertia were set up on the General Electric Company differential analyzer.[1] Since it was desired to investigate a number of different operating conditions, the machine characteristics were held constant in all but a few cases. These characteristics given in Appendix B are those of a low-resistance motor which tend to accentuate the motor transients. The effect of increased rotor resistance may be seen in Figure 15. The results and conclusions presented in this paper indicate the importance of considering the transient electrical and mechanical phenomena simultaneously.

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The authors express their appreciation to S. B. Crary for having suggested this problem and for his constructive criticism; to C. Concordia for his suggestions and discussion; and to members of the staff of the differential analyzer.

Assumptions

The analysis and results are based on the following assumptions (which are similar to those of reference 2).

1. Balanced windings are assumed on both rotor and stator.

2. The mutual inductance between any stator and rotor winding is considered to be a cosinusoidal function of the electrical angle between the axes of the two windings.

3. It is assumed that the rotor is smooth and that the self-inductance of any winding is independent of rotor position.

4. The effects of saturation, hysteresis, and eddy currents are neglected.

5. The machine impedances are assumed to be independent of rotor speed.

6. The machine electrical neutral is assumed ungrounded.

7. In all cases studied, the motor is considered connected to an infinite bus through negligible impedance. The effect of system impedance can be taken into account by increasing the stator impedance, that is, by moving the infinite bus from the terminals of the machine and placing it further back in the system.

Conclusions

1. Large errors may be introduced by using the steady-state speed-torque curve for the calculation of transients.

2. Under transient conditions, motor torque can be developed at speeds above synchronous, and generator torque can be developed at speeds below synchronous.

3. Transient electric torques under shock mechanical loads or sudden electrical disturbances can be several times the steady-state pullout torque.

4. Transient electric-torque oscillations incident to a sudden change in applied voltage are, in general, more severe than those due to a sudden change in mechanical load.

5. The initial rate of change of electric torque with respect to speed following a sudden change in mechanical load is less than the steady-state speed-torque curve would indicate.

6. For a sudden change in applied voltage the transient electric torque is unaffected by the electrical angle at which the voltage is changed, provided that the machine was not initially running open-circuited with trapped flux.

7. Transient-torque oscillations following the sudden application of voltage to a machine containing trapped flux (as in jogging) can be much more severe than the transient torques incident to a short circuit at the motor terminals.

8. With a machine temporarily disconnected from the system and running with trapped rotor flux, there are definite optimum times for reapplication of voltage in order to reduce the torque oscillations.

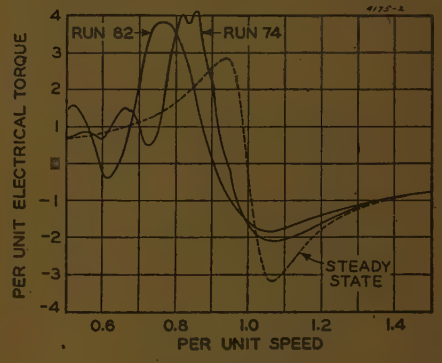


Figure 2. Per unit electric torque versus per unit speed

Effect of rotor deceleration. Transient speed–torque curves with constant rotor deceleration from steady-state conditions at 1.5 per unit speed
Run 82—12 cycles for unit speed change
Run 74—24 cycles for unit speed change

9. The magnitude of transient plugging torque is greatly affected by the magnitude of the trapped rotor flux and its position relative to the stator. It is also affected by motor speed.

10. Transient currents caused by jogging or plugging (with trapped rotor flux) may be larger than the steady-state full-voltage starting current.

Method of Solution

The equations of induction-motor performance (see equations 10 through 20, Appendix A) were set up on the General Electric Company differential analyzer.[1] An inspection of these equations will show that 14 separate integrations were required in the solution. In this respect the problem provided an excellent oppor-

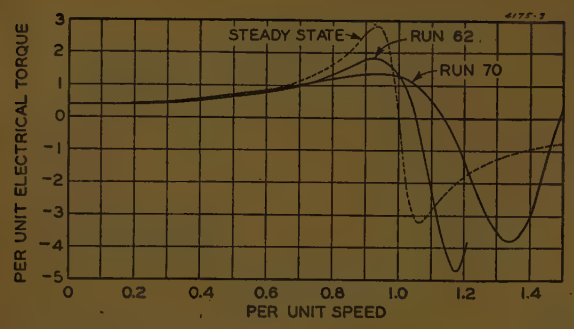
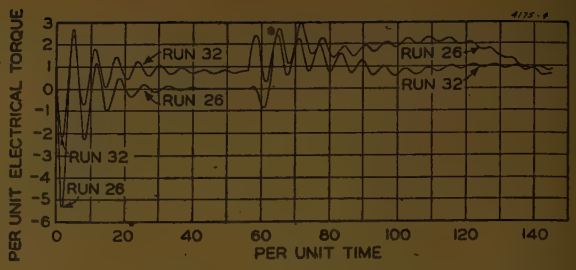


Figure 3 (left). Per unit electric torque versus per unit speed

Figure 4 (right). Per unit electric torque versus per unit time (electrical radians)



Effect of rotor acceleration
Transient speed–torque curves with constant rotor acceleration from steady-state conditions at zero speed
Run 70— 6 cycles for unit speed change
Run 62—24 cycles for unit speed change

Effect of voltage disturbance; steady-state full-voltage conditions for 0.99 per unit speed before zero time
Run 32—Voltage reduced to 0.5 per unit at $t=0$. Voltage restored to 1.0 per unit at $t=18\pi$ (9 cycles)
Run 26—Terminals short circuited at $t=0$. Voltage restored to 1.0 per unit at $t=18\pi$ (9 cycles)

tunity to observe and judge the operation of the analyzer, when using the entire integrator capacity. Five quantities, obtained as outputs from the analyzer, were plotted as functions of time. These were:

(*a*). One of the applied voltages, E_α.*

(*b*). Phase α stator current, I_α.

(*c*). Phase β stator current, I_β.

(*d*). Rotor speed, $p\theta$.

(*e*). Electric torque, T_e.

As shown in Appendix C, the analyzer results were checked against calculated results in two cases which were readily soluble analytically. Differences between analyzer and calculated results were in both cases negligible.

Discussion of Results

A. Electric Torques Caused by Rotor Acceleration — Transient Torque-Speed Curves

In order to study the transients due to changing rotor speed as differentiated from those due to electrical disturbances, the machine was subjected to various constant rotor accelerations and decelerations. The electric torques developed in some of these cases are shown in Figures 1, 2, and 3.

In Figure 1, the motor was assumed in all three cases to be operating initially under steady-state conditions with a slip of one per cent. The rotor was then decelerated in runs 71, 81, and 73 at rates corresponding to unit change of speed in 6, 12, and 24 cycles, respectively, of fundamental frequency. It is of interest to note that the initial rate of change of torque with speed is zero and that the electric torque oscillates about the steady-state speed-torque curve. That the initial rate of change of torque with speed is zero, of course, is due to inductive lags in the motor. The relation of the first swings of these curves may be explained as follows:

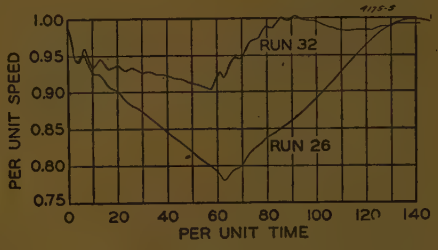


Figure 5. Per unit rotor speed versus per unit time (electrical radians) for cases shown in Figure 4

1. If the rotor were decelerated at an infinitely slow rate, the torque would follow its steady-state speed-torque curve.

2. If the rotor were decelerated at an infinitely fast rate, the torque would remain constant at its initial value because of inability of the motor to respond electrically to the infinite deceleration.

It is seen, then, that the speed-torque curves of runs 71, 81, and 73 appear in a logical sequence between the limiting curves resulting from zero and infinite deceleration.

Figure 1 also shows that appreciably higher-peak electric torque may be developed upon the sudden application of mechanical load than the steady-state theory would indicate, and at a different speed.

Figure 2 illustrates results obtained when the motor was decelerated from initial steady-state conditions for a rotor speed of 1.5 (per unit).

The curves shown in Figure 3 resulted from acceleration of the rotor from steady-state standstill conditions.

This group of curves illustrates the fact that motor torque may be developed above and generator torque below synchronous speed. It also indicates that large errors may be introduced by using the steady-state speed-torque curve for the calculation of severe transients. Acceleration and deceleration tests have been proposed as a method of obtaining steady-state characteristics.[3] The present results show that there is a limitation on the magnitude of acceleration which can be used in such tests if the true steady-state behavior is to be obtained.

B. Electric Torques Due to Sudden Change in Applied Voltage

Figure 4 shows the effect upon electric torque of sudden changes in applied voltage. For both runs 32 and 26 of Figure 4, the motor was initially operating at steady-state conditions with a slip of one per cent. A constant mechanical load torque was assumed in both cases, although arbitrary load-torque curves as a function of speed easily could have

* See Appendix B for list of nomenclature.

Shows one cycle of steady-state current before zero time

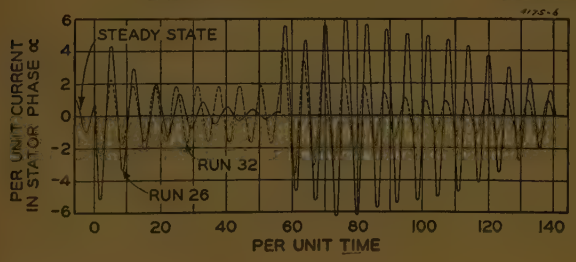
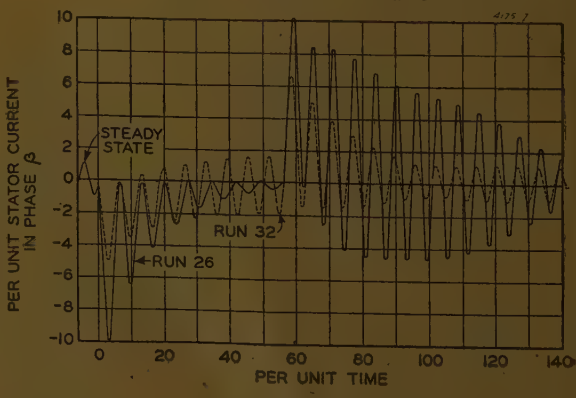


Figure 6 (left). Per unit stator current in phase α versus per unit time (electrical radians) for cases shown in Figure 4

Figure 7 (right). Per unit stator current in phase β versus per unit time (electrical radians) for cases shown in Figure 4

Shows one cycle of steady-state current before zero time



been used if desired. An inertia constant (H) of approximately 0.45 was assumed for motor and load combined.

In run 26, at $t=0$, (also $E_\beta=0$), the applied voltage suddenly was reduced to zero, corresponding to a zero-impedance short circuit at the motor terminals. As is seen from an inspection of Figure 4, the maximum electric torque following the application of a short circuit is of the order of five per unit. It is also seen, as would be expected, that the electric torque became zero after the transients had died out and remained zero until the short circuit was removed. The fault was removed nine cycles after its application. The torque recovery following the removal of short circuit also is pictured in Figure 4.

Run 32, which also has been plotted in Figure 4 for purposes of comparison, is similar to run 26, except that the applied voltage was reduced suddenly to half value rather than to zero.

Several runs also were made in which the electrical angle at which the voltage was reduced or restored was varied. As stated in the conclusions, the electrical angle of switching under these conditions had no effect on the ensuing electric torque for a given switching time.

Figure 5 shows the per unit speed as a function of time for runs 26 and 32. The two-phase stator currents I_α and I_β for these two runs are plotted in Figures 6 and 7, respectively. As shown in Appendix A, corresponding three-phase currents may be obtained from I_α and I_β by means of a relatively simple manipulation; in fact, in the present treatment I_α itself is one of the three-phase currents.

C. Plugging Torques

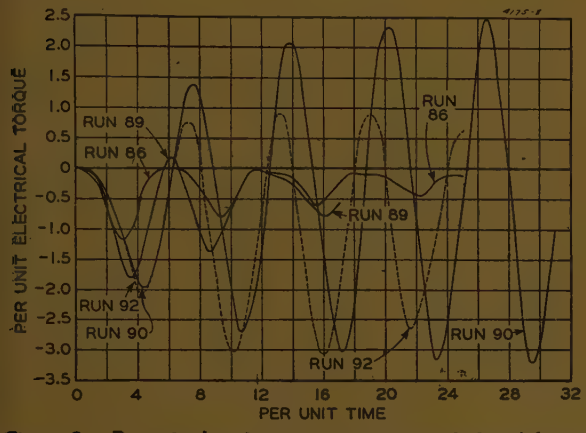
Two extreme plugging conditions are those in which the machine is plugged instantaneously while running at rated speed and load, and while it is running at rated speed but after the rotor flux has died out completely. Plugging torques from runs which are nearly these extremes are shown in Figure 10.

Run 85 was obtained as follows:

1. With the motor running at steady-state conditions with a slip of one per cent, the stator phase β was opened at a zero of I_β.

2. Phase α of the stator was opened approximately one-fourth cycle later at a zero of I_α.

3. At the same instant that phase α was opened, voltage of opposite (negative) phase rotation was applied simultaneously to both stator phases. (In this case, the mechanical load torque was assumed to be constant at its initial value, and the inertia constant (H) was assumed to have a value of approximately 1.43.) This is obviously almost one of the extreme cases mentioned before.



Effect of rotor speed on plugging torque. Infinite-inertia machine running at various positive speeds with zero rotor flux. Unit voltage of negative phase rotation applied at zero time

Run 86—Rotor speed = 1.00 per unit
Run 89—Rotor speed = 0.50 per unit
Run 92—Rotor speed = 0.15 per unit
Run 90—Rotor speed = 0 per unit

Figure 8. Per unit electric torque versus per unit time (electrical radians)

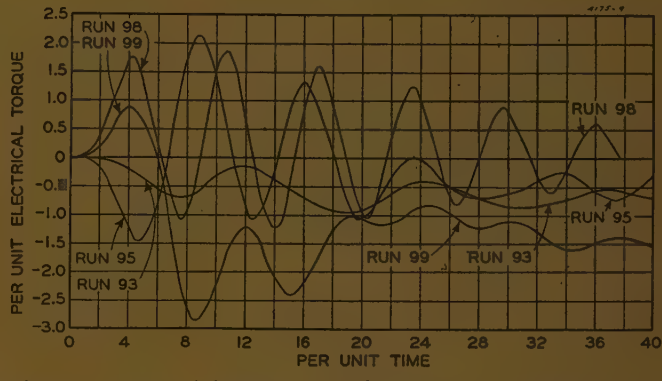


Figure 9. Per unit electric torque versus per unit time (electrical radians)

Effect of rotor speed on plugging torque. Infinite-inertia machine running at various negative speeds with zero rotor flux. Unit voltage of negative phase rotation applied at zero time

Run 95—Rotor speed = −0.25 per unit
Run 93—Rotor speed = −0.50 per unit
Run 99—Rotor speed = −0.75 per unit
Run 98—Rotor speed = −1.00 per unit

The two phase currents for this run are on Figures 11 and 12.

Run 86 was obtained by applying negative-phase-rotation voltage to an infinite-inertia machine running at synchronous speed with zero rotor flux. It is the other extreme case and is compared with run 85. The effect of trapped rotor flux is seen to be very important.

It is interesting to note the similarity between run 86 of this paper and run 36 of reference 4, in which the machine constants are similar to those used in this study.

Figures 8 and 9 have been included to show the effect of rotor speed upon plugging torque. All curves shown in these two figures are based on the assumptions that the motor was initially unexcited, contained no trapped flux, and had infinite rotor inertia, negative phase rotation of voltage being applied in all cases.

Figure 9 shows torques which are not, strictly speaking, "plugging" torques since the phase rotation of the applied voltage is in the same direction as the motor rotation. However, they are of interest in that they extend the range of rotor speed. Such conditions may be encountered in some pump and propeller drives having more than one motor on the same shaft.

D. Reclosing of Stator Circuit With Trapped Flux in the Rotor

Several cases were studied in which the stator was open-circuited from a steady-state full-load running condition. After several cycles, full voltage was reapplied. Figures 13 and 14 show the electric torque and speed respectively for a few of these cases. It will be seen from these curves that there is at least one definite optimum reclosing time both from the standpoint of torque oscillations and from that of speed-recovery time. That there are several of these optima, and that they depend on machine inertia and rotor resistance may be seen from the following analysis.

In the steady state with the motor running at approximately one per cent slip, the rotor flux rotates at synchronous speed and lags the stator flux by a definite angle. It is continually slipping forward with respect to the rotor. When the stator is open-circuited, the rotor flux becomes fixed in the rotor and decays relatively slowly (with the rotor open-circuit time constant L/r_r) from its initial value. Since the electric torque is zero, the rotor slows down so that the trapped and slowly decaying rotor flux lags at an increasing rate behind the

steady-state position of the stator flux.

An understanding of the phenomena occurring when the stator circuit is again closed on the supply voltage may be obtained by regarding the induction motor as a synchronous machine running below synchronism, the trapped rotor flux taking the place of the synchronous-machine excitation. The sudden reapplication of stator voltage produces two fluxes, one very nearly the steady-state rotating field, the other a stationary (and decaying) field. Now the machine time constants are much shorter than with the stator open-circuited, so that the stationary field components decay more rapidly, but for the purposes of the argument one may neglect this decay. Both stator fields react on the rotor flux, the steady-state rotating component producing a slip-frequency torque, the stationary component producing a speed-frequency component. If the slip is small, the slip-frequency component acts roughly as a "synchronizing" torque, so that it is evident that there is a preferred initial position of the rotor for minimum time to recover speed, while the speed-frequency component produces only a superposed vibration of the rotor speed. One may calculate from the initial slip and rate of increase of slip the relative angle through which the rotor has passed, and it then will be found that at first for very small angles of retardation the speed will recover quickly and with a minimum of disturbance. Then, as the angle exceeds 90 degrees, the initial accelerating torque becomes smaller and finally is not sufficient, so that the machine speed at first may decrease further by an appreciable amount. As in the equivalent synchronous-machine case, the "worst" angle lies only a relatively short distance behind the "best" angle. The situation is made more complex by the fact that in every slip cycle the slip becomes greater and the trapped flux becomes smaller, so that the variation with angle becomes much less pronounced. The present case has a rather low rotor

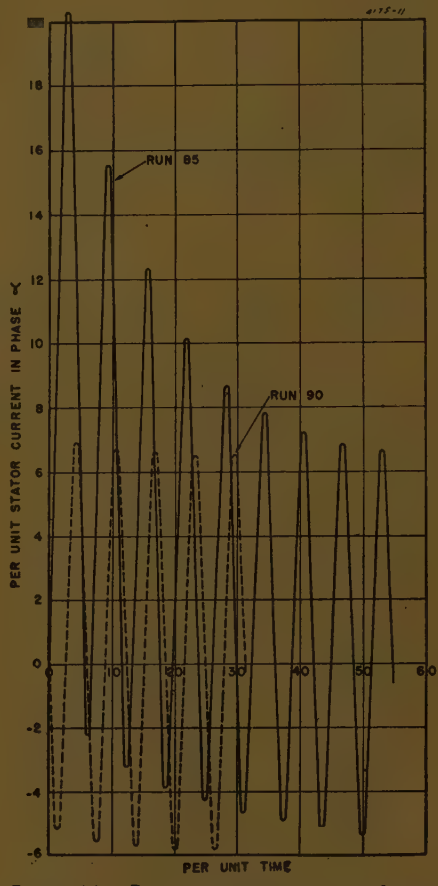


Figure 11. Per unit stator current in phase α versus per unit time (electrical radians) for runs 85 and 90

See Figures 10 and 8, respectively, for corresponding electric torque

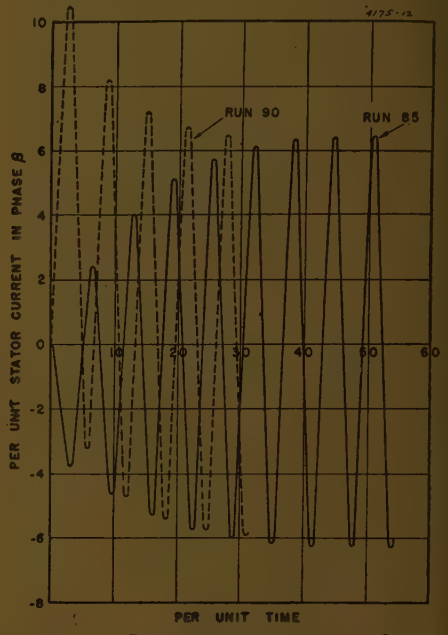


Figure 12. Per unit stator current in phase β versus per unit time (electrical radians) for runs 85 and 90

See Figures 10 and 8, respectively, for corresponding electric torque

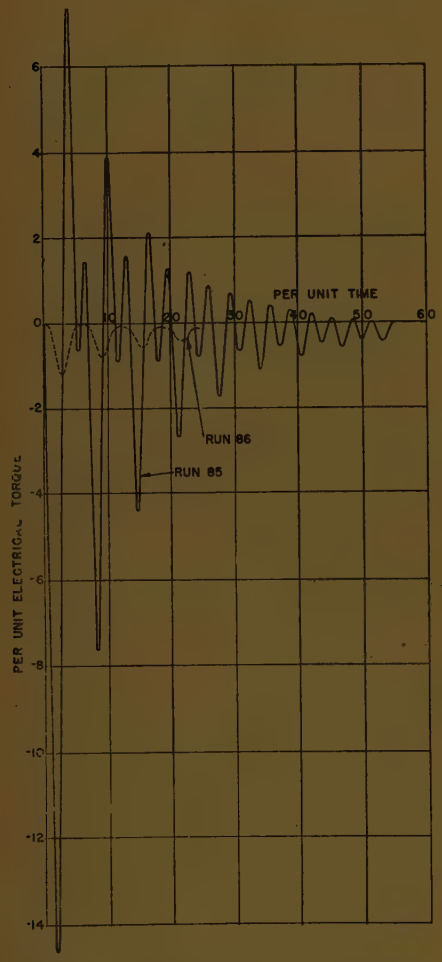


Figure 10. Per unit electric torque versus per unit time (electrical radians)

Effect of trapped rotor flux on plugging torque
Run 85—Plugging from steady-state conditions for 0.99 per unit speed (see section C)
Run 86—Plugging an infinite-inertia machine running at synchronous speed with zero rotor flux

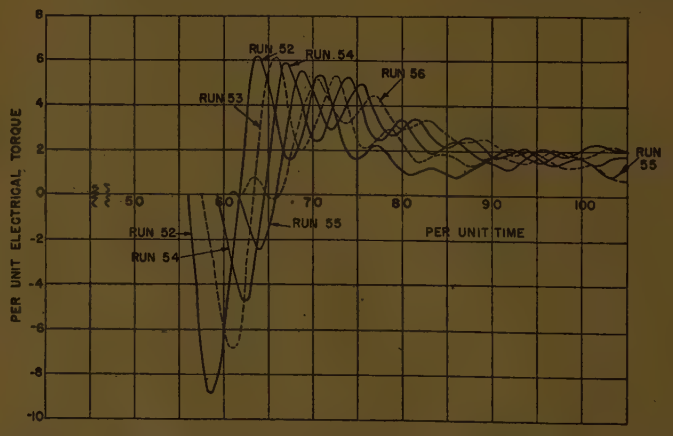


Figure 13. Per unit electric torque versus per unit time (electrical radians)

Effect of switching angle. Steady-state full-voltage conditions for 0.99 per unit speed before zero time. Stator open-circuited at $t=0$
Run 52—Voltage restored to 1.0 per unit at $t=18\pi$ (9 cycles)
Run 53—Voltage restored to 1.0 per unit at $t=18.5\pi$ ($9\frac{1}{4}$ cycles)
Run 54—Voltage restored to 1.0 per unit at $t=19\pi$ ($9\frac{1}{2}$ cycles)
Run 55—Voltage restored to 1.0 per unit at $t=19.5\pi$ ($9\frac{3}{4}$ cycles)
Run 56—Voltage restored to 1.0 per unit at $t=20\pi$ (10 cycles)

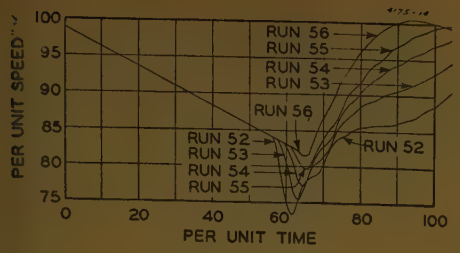


Figure 14. Per unit speed versus per unit time (electrical radians) for runs 52–56

See Figure 13 for corresponding electric torque

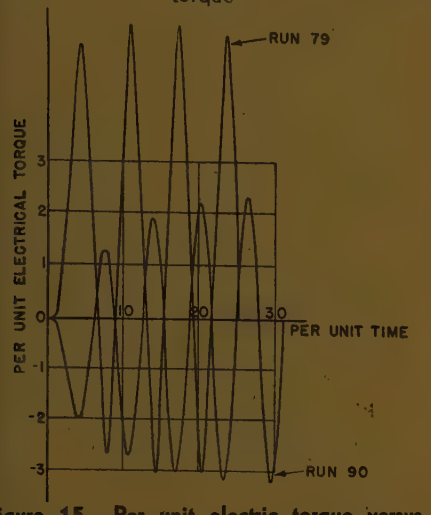


Figure 15. Per unit electric torque versus per unit time (electrical radians)

Effect of rotor resistance. Full voltage applied to locked rotor machine

Run 79—Rotor resistance = 0.04 per unit. Positive-phase-rotation voltage

Run 90—Rotor resistance = 0.01 per unit. Negative-phase-rotation voltage

resistance, so that the effect is still large in the second slip cycle which has been studied. The inertia of the machine evidently enters here also, as it determines the rate of increase of slip. A motor with a small inertia and a high rotor resistance would show little variation with angle, and the ratio of inertia to rotor resistance may form a rough criterion of the importance of the effect of angle. Also if the slip becomes very large, the effect of the stationary stator flux may be emphasized and a pronounced speed decrease consequently may be observed. In all cases, the severity of the rotor swings should tend to be less than in the synchronous-machine case because of the generally higher resistances and the fact that the rotor has no sustained rotor excitation and thus no preferred position in which it must come to equilibrium.

E. Transient Currents

If we consider only the a-c component of current, it is seen from Figures 6 and 7 that the current caused by a terminal short circuit is somewhat less than full-voltage starting current; however, that resulting when the fault is removed is approximately equal to full-voltage starting current.

It is interesting to note that the current caused by plugging (when the rotor contains trapped flux) may be much larger than full-voltage starting current. For the case illustrated in Figures 11 and 12, the maximum current is nearly twice the normal starting value.

Appendix A. System Equations

The equations whose solution forms the basis for the results of this study have been derived by Stanley.[2] These equations in which all quantities have been referred to the stator may be written

$$e_\alpha = p\psi_\alpha + r_r i_\alpha + \psi_\beta(p\theta) = 0 \quad (1)$$

$$e_\beta = p\psi_\beta + r_r i_\beta - \psi_\alpha(p\theta) = 0 \quad (2)$$

$$\psi_\alpha = G(p)E_\alpha + x(p)i_\alpha \quad (3)$$

$$\psi_\beta = G(p)E_\beta + x(p)i_\beta \quad (4)$$

$$T_e = \psi_\beta i_\alpha - \psi_\alpha i_\beta \quad (5)$$

$$p\theta = \frac{1}{H'}\int (T_e - T_m)dt \quad (6)$$

$$\theta = \int p\theta\, dt \quad (7)$$

$$E_\alpha = E\cos t \quad (8)$$

$$E_\beta = E\sin t \quad (9)$$

where

$$G(p) = \frac{M}{Lp + r_s}$$

$$x(p) = \frac{L(x'p + r_s)}{Lp + r_s}$$

$$x' = L - \frac{M^2}{L}$$

All quantities are defined in Appendix B.

For solution on the differential analyzer, these equations are written in the following form:

$$\psi_\alpha = -r_r\int i_\alpha dt - \int \psi_\beta d\theta \quad (10)$$

$$\psi_\beta = -r_r\int i_\beta dt + \int \psi_\alpha d\theta \quad (11)$$

$$x'i_\alpha = \psi_\alpha + \int\left(\frac{r_s}{L}\psi_\alpha - \frac{M}{L}E_\alpha - r_s i_\alpha\right)dt \quad (12)$$

$$x'i_\beta = \psi_\beta + \int\left(\frac{r_s}{L}\psi_\beta - \frac{M}{L}E_\beta - r_s i_\beta\right)dt \quad (13)$$

$$E_\alpha = -\int E_\beta dt \quad (14)$$

$$E_\beta = \int E_\alpha dt \quad (15)$$

$$T_e = \int \psi_\beta di_\alpha + \int i_\alpha d\psi_\beta - \int \psi_\alpha di_\beta - \int i_\beta d\psi_\alpha \quad (16)$$

$$p\theta = \frac{1}{H'}\int (T_e - T_m)dt \quad (17)$$

$$\theta = \int p\theta\, dt \quad (18)$$

Equations 10 through 18 which require 14 integrators were set up on the differential analyzer, and a number of solutions was obtained. The quantities plotted were electric torque, rotor speed, applied voltage, and the two components of stator current. The latter do not appear explicitly in equations 10 through 18 but come directly from the definition of flux; namely:

$$I_\alpha = \frac{1}{M}(\psi_\alpha - Li_\alpha) \quad (19)$$

$$I_\beta = \frac{1}{M}(\psi_\beta - Li_\beta) \quad (20)$$

From the definitions of the α and β quantities,[2] the stator-phase currents for a three-phase machine in terms of I_α and I_β are (with no zero-sequence currents):

$$i_a = I_\alpha \quad (21)$$

$$i_b = -\frac{1}{2}I_\alpha + \frac{\sqrt{3}}{2}I_\beta \quad (22)$$

$$i_c = -\frac{1}{2}I_\alpha - \frac{\sqrt{3}}{2}I_\beta \quad (23)$$

Appendix B. Nomenclature and Values of Parameters for Machine Studied

All quantities are in per unit on kilovolt-ampere base.

L = apparent three-phase stator and rotor self-inductance = 3.07
M = apparent three-phase mutual inductance = 2.99
r_s = resistance of one stator phase = 0.01
r_r = resistance of one rotor phase = 0.01
$p\theta$ = rotor speed
t = time in electrical radians at unit speed
θ = electrical angle between rotor phase α and stator phase α at any time t
$p = d/dt$
I_α = stator current in positive α axis
I_β = stator current in positive β axis
E_α = stator terminal voltage in positive α axis
E_β = stator terminal voltage in positive β axis
E = maximum value of terminal voltage
ψ_α = rotor-flux linkages in positive α axis referred to stator
ψ_β = rotor-flux linkages in positive β axis referred to stator
i_α = rotor current in positive α axis referred to stator
i_β = rotor current in positive β axis referred to stator
e_α = rotor terminal voltage in positive α axis referred to stator = 0
e_β = rotor terminal voltage in positive β axis referred to stator = 0
H' = rotor moment of inertia = 338.0
H = conventional inertia constant $= H'/4\pi$ (frequency) = 0.448
T_e = induction-motor torque
T_m = mechanical load torque = 0.91
i_a = stator current in phase a
i_b = stator current in phase b
i_c = stator current in phase c
λ_1, λ_2 = roots of characteristic equation (locked rotor)

$\omega = 2\pi \times$ fundamental applied frequency $= 1.0$

ϕ = power-factor angle (locked rotor)

$$= \tan^{-1} \frac{\lambda_1\lambda_2 - \omega^2}{\omega(\lambda_2 + \lambda_1)}$$

$$\theta_1 = \tan^{-1} \frac{\omega^2 + \lambda_1\lambda_2}{\omega(\lambda_2 - \lambda_1)}$$

Appendix C

As a check on the differential-analyzer accuracy, the torque–time curves of two of the simple cases were checked by calculation. An exact expression for the electric torque with blocked rotor and simultaneous application of voltage in both axes (on the basis of the given assumptions) is found in equation 5 of reference 5. This equation may be reduced to

$$T_e = T_s \left[1 + e^{-(\lambda_2\lambda_3)t} - \frac{e^{-\lambda_1 t} \cos(\omega t + \theta_1) + e^{-\lambda_2 t} \cos(\omega t - \theta_1)}{\cos \theta_1} \right]$$

where

$$\theta_1 = \tan^{-1} \frac{\lambda_1\lambda_2 + \omega^2}{\omega(\lambda_2 - \lambda_1)}$$

This is identical in form with the approximate equation 11 of reference 6, the difference being found in the phase angle θ_1 which replaces ϕ of Kilgore and Wahl[6] where ϕ is

$$\phi = \tan^{-1} \frac{\omega^2 - \lambda_1\lambda_2}{\omega(\lambda_2 + \lambda_1)}$$

This difference amounts to three per cent in the maximum torque for the machine constants used in the greater part of the study. The calculation of torque was carried out for four or five cycles of each of two cases (runs 90 and 79) Figure 15, and the discrepancy between calculated and analyzer solutions was found to be negligible. The two-phase currents corresponding to run 90 are given in Figures 11 and 12.

References

1. A New Differential Analyzer, H. P. Kuehni, H. A. Peterson. AIEE Transactions, volume 63, 1944, May section, pages 221–8.

2. An Analysis of the Induction Motor, H. C. Stanley. AIEE Transactions, volume 57, 1938, pages 751–5.

3. An Application of Deceleration Test Methods to the Determination of Induction-Motor Performance, R. W. Ager. AIEE Transactions, volume 58, 1939, February section, pages 72–6.

4. Transient Torques in Squirrel-Cage Induction Motors With Special Reference to Plugging, E. S. Gilfillan, Jr., Edward Kaplan. AIEE Transactions, volume 60, 1941, pages 1200–09.

5. Discussion by S. B. Crary of Transient Starting Torques in Induction Motors. AIEE Transactions, volume 59, 1940, page 1263.

6. Transient Starting Torques in Induction Motors, A. M. Wahl, L. A. Kilgore. AIEE Transactions, volume 59, 1940, November section, pages 603–07.

Recent Advances in Aircraft Tachometer Design

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Synopsis: A discussion of recent developments in magnetic-drag-type aircraft tachometer equipment. Wartime demands for increased temperature range, instrument durability, and compactness have led to certain revisions and new design features. The results of these recent developments are outlined in this paper.

THE magnetic-drag tachometer system consists of a three-phase generator supplying power to a synchronous motor in an indicator (Figures 1 and 2). The generator is mounted directly on the airplane engine and the indicator is mounted on the instrument panel. The motor in the latter revolves an assembly of permanent magnets which imparts torque to an aluminum-alloy drag disk, thereby moving the indicator pointer over the scale until an equal and opposite restraining torque is developed in the control spring.[1] The generators and indicators are interchangeable with other instruments of the same manufacture and similar units made by other concerns.

During the past three years several new requirements have influenced the design and manufacture of this aircraft tachometer equipment: the operating temperature range has increased by nearly 50 per cent; the vibration conditions which these devices must withstand have become more severe with the production of faster and more powerful engines; and increased instrumentation has made better utilization of panel space essential. Revisions in generator and indicator design have resulted from the new temperature and vibration requirements. A new dual-element tachometer, occupying the space as one single indicator, has been developed to save panel area.

Generators

A cut-away view of a tachometer generator is illustrated in Figure 3; this readily illustrates the simple, sturdy construction of the generator. To obtain satisfactory operation at temperatures down to −65 degrees centigrade, a special low-temperature lubricant is now used in the ball bearings. The resulting torque required to drive the generator at −65 degrees centigrade is shown in Figure 4 for single- and double-element indicator loads.

With increased speed and power output of aircraft engines, the generator must withstand more severe vibration than ever previously encountered; and modifications in design now eliminate consequent hazards. The possibilities of lead breakage are eliminated by more secure clamping of leads; oil leakage from the engine into the generator winding is avoided by better oil seals and by additional vent-holes in the end-shield, preventing the build-up of oil pressure in front of the ball bearing at the flange-mounted end of the generator. Increased strength of the front end shield is assured by additional ribs, and the former two-piece receptacle end-shield is now a stronger and simpler one-piece casting.

The tachometer generator drive key is required to withstand accelerations of 400 revolutions per second per second. Generators of the present construction have withstood repeated accelerations as great as 800 revolutions per second per second without damage. In certain installations where unusually severe pulsations of the driving system are present, the drive-key design is further modified to prevent excessive generator wear and resulting pointer fluctuations in the indicator. This drive key includes a short section of flexible tachometer cable. The square driving tang is split diagonally through the center line and the two sections are spread so that no play is present between the drive key and the mating part on the engine. By this change in construction no indicator pointer fluctuation occurs after many hours' operation where previously the indication became unsatisfactory during the first 10 to 20 minutes of use.

Representative generator-voltage-output–speed curves are shown by Figure 5 for conditions of no load, one and two indicator element loads.

Indicators

The range of operating temperature for tachometer indicators has increased, upward from +45 degrees centigrade to +70 degrees centigrade, and downward from −35 degrees centigrade to −55 degrees centigrade. This increase in range

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Figure 1. Tachometer equipment

Left to right: Tachometer generator; single-element indicator, 3½ revolutions; dual-element indicator; single-element indicator (330 degrees); synchroscope

from a total of 80 to a total of 120 centigrade degrees requires improved temperature compensation material so that the error in indication is not excessive.

Improvements in commercially available temperature-sensitive nickel-steel compensator material provide the typical temperature compensation curve shown in Figure 6; the uncompensated curve shows the change in indication (at the 3,000-rpm point) before the addition of compensator material, and the compensated curve shows that this error is reduced to less than one per cent by proper compensation.

Due to the high-ambient-temperature operation, it is essential that the lubricant for the indicator motor bearings have a low evaporation rate. To meet this requirement the indicator ball bearings are lubricated with inhibited blackfish jaw oil applied in controlled quantities. With this lubrication individual indicators have operated continuously for as much as 20,000 hours.

Room - temperature torque – speed curves for representative tachometer indicator motors are shown in Figure 7. The starting or pull-in torque is developed jointly by a permanent-magnet rotor and eddy-current hysteresis disk.

The vibration test for tachometer indicators consists of movement of the indicator on a 0.018–0.020-inch-diameter circle in a plane inclined 45 degrees to the horizontal. The top frequency vibration has been increased from 2,500 to 3,000 cycles per minute. All indicators are subjected to this vibration test after assembly to assure freedom from excessive pointer oscillations or changes in indication.

Dual-Element Indicator

As airplanes increased in size and complexity, the available panel space has been needed for more and more indications; thus, where previously a single-element indicator was installed it has become essential to build two or more indicators in one case wherever possible. This need leads to the development of the dual-element tachometer indicator, shown in Figure 1, to show the speed of two engines in the same panel space as was previously required for one engine. A cut-away view of this dual indicator, with one element removed, is shown in Figure 8.

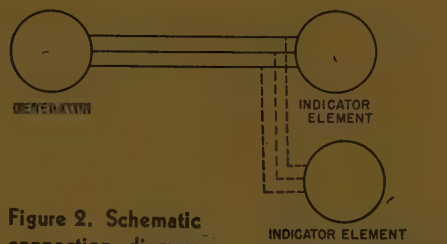


Figure 2. Schematic connection diagram



Figure 3. Cut-away view of generator

The dual indicator presented problems of much smaller-size motors, smaller rotating-magnet assemblies, and a means of coupling the two elements to a concentric pointer assembly. Previous experience with the single-element indicator showed the following adjustments to be desirable:

(*a*). Zero adjustment.

(*b*). Full-scale adjustment.

(*c*). Balancing means.

(*d*). Adjustable temperature compensation.

In addition cam adjustment of gear mesh has been built into each element of the dual indicator to facilitate adjustment and maintenance.

A special feature of the design is the possibility of disassembly into interchangeable subassemblies. These subassemblies may be adjusted, tested, and inspected as units and subsequently assembled into completed tachometers. The motor and magnetic-drag assembly are completely assembled and tested in the normal manufacturing procedure, thereby preventing expensive tear-downs at later positions. Field maintenance and handling is greatly facilitated by these types of construction.

Smaller Motor

For the synchronous motor of the dual indicator, a recently developed magnetic material, copper-nickel-cobalt, is used for the permanent-magnet rotor instead of the sintered-oxide material utilized in the single-element indicator. The new material offers the advantages of improved mechanical strength, greater ease of fabrication, and simpler assembly.

The stator of the indicator motor is of the conventional three-phase type. To decrease the temperature rise of the windings through greater thermal conductivity, glass-cloth slot insulation replaces the fiber type common to the single-element indicator. This change reduces the temperature rise by approximately ten per cent.

Drag Magnet Assembly

The necessity of providing two indicator elements in the space previously occupied by one necessitates smaller rotating magnets. In order to obtain sufficient torque for the moving element assembly, a second new magnet material is utilized. This material, Alnico V, produces approximately twice the external energy per unit volume of Alnico II used in the single-element indicator. Major differences in handling the two types of Alnico in manufacturing are the necessity for initial magnetization of the Alnico V during cooling from a semi-molten state, and the requirement that Alnico V be carried through a much greater temperature stabilization cycle than for Alnico II in order to prevent sub-

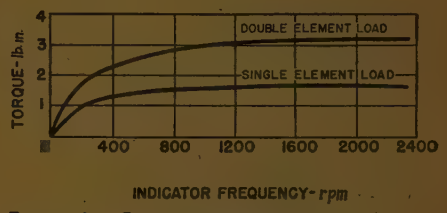


Figure 4. Generator driving torque at −65 degrees centigrade

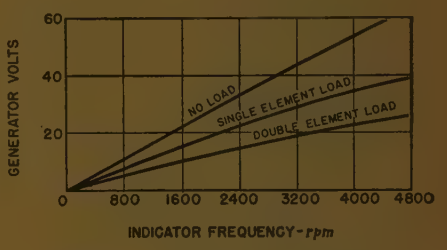


Figure 5. Generator voltage output

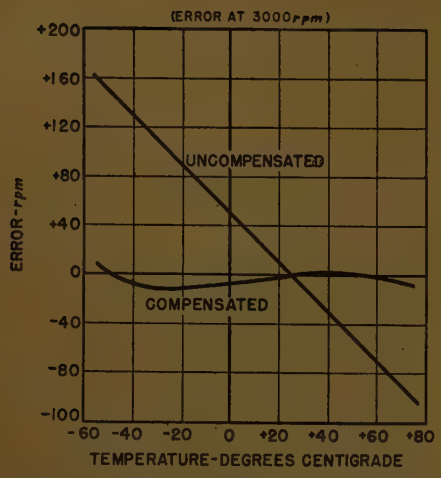


Figure 6. Typical temperature compensation

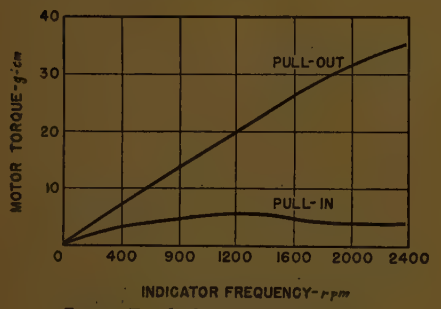


Figure 7. Indicator-motor torque

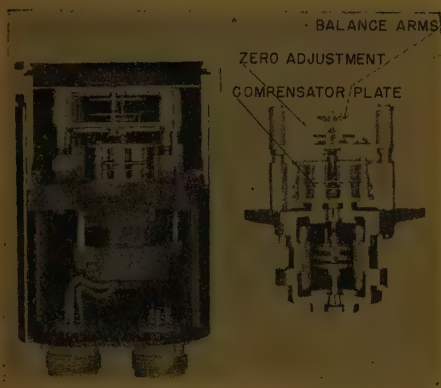


Figure 8. Cut-away view of dual indicator

sequent changes in magnetic strength throughout the instrument operating temperature range of −55 to +70 degrees centigrade.[2,3]

Calibration adjustment of the indicator may be accomplished either by magnetic knockdown with an a-c coil or by varying the gap between the two sets of magnets. The latter adjustment is normally used for only small changes in calibration and is readily made with the

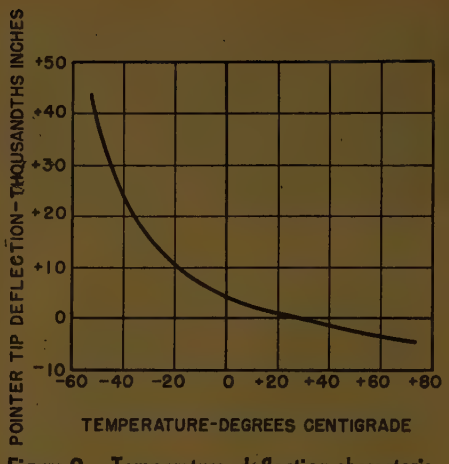


Figure 9. Temperature–deflection characteristics of single-coated pointers

instrument cover removed. Calibration adjustment with a demagnetizing coil may be made to either element of the assembled indicator without affecting the calibration of the other element, although the magnets in the two assemblies are separated by less than three quarters of an inch. This method is ideal since the element being adjusted may be in operation while the demagnetizing is being done. Temperature compensation adjustment is provided by movement of the compensator plate (Figure 8) along the length of the magnets in order to shunt more or less flux.

Moving Elements

The zero-adjusting arm and balance arms are readily discernible in Figure 8. The mechanical assembly of gear hubs to shafts as small as 0.031 inch in diameter is accomplished by means of a knurl on the shaft; thus assuring a tight, permanent fit.

The concentric pointer shaft assembly (Figure 8) is supported at the ends by ring-stone jewels. Intermediate supports are provided by two additional ring-stone jewels mounted in the outer bearing sleeve with the first one supporting the front end of the inner shaft and the second the back end of the outer sleeve. The advantages of this assembly are its inherent line-up and a single end-play adjustment for the complete subassembly.

Prior to the increase in range of temperature operation, tachometer pointers were normally flat and painted with luminescent material on one side only. Changes in temperature cause bending of the pointer because of the difference in expansion and contraction of the aluminum pointer material and the fluorescent

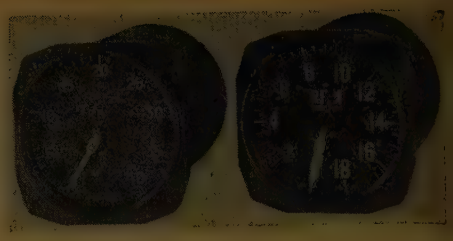


Figure 10. Dual tachometers for supercharger and helicopter applications

material. The amount of deflection occurring for a 0.010-inch-thick pointer $1^3/_{16}$ inches in length is shown in Figure 9. In order to improve this condition the pointer for the single-element indicator has been "ribbed" to reduce this bending action to less than half of its former value. Because of the small space between the two pointers on the dual element indicator, its pointers are made flat but are painted on both front and back sides, thus eliminating virtually all deflection.

Conclusion

The several problems of increased temperature range, additional vibration, and superior panel utilization have been met. Existing designs have been improved and new designs developed to accomplish the stringent wartime requirements.

In addition to the furthered development in magnetic materials and processes fostered by the designing of the tachometer equipment, certain unique applications have been made. Among the present and future applications are included airplane supercharger and helicopter rotor speed indication (Figure 10), test panel equipment, regulator control, and timing apparatus. A single indicator element from the dual tachometer can be built into a $1^7/_8$-inch diameter case for use where reduced size is paramount, and the separate indication of engine speed is desired.

By means of these design revisions it has been possible to meet the new requirements which aircraft tachometer systems have encountered.

References

1. The Magnetic-Drag Tachometer, R. G. Ballard. AIEE Transactions, volume 61, 1942, pages 366–8.

2. Influence of New Magnet Steels on Design of Electrical Indicating Instruments, I. F. Kinnard, J. H. Goss. *The Instrument Maker*, volume 11, July–August 1943, pages 16–18.

3. Alnico—Its Properties and Possibilities, J. Q. Adams. *General Electric Review*, volume 41, December 1938, pages 518–22.

Automatic Control and Switching Equipment for Capacitor Banks and Its Application

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THE increasing use by utilities or industrial plants of shunt capacitors for voltage improvement, power-factor correction, and release of circuit capacity is leading in many cases to the desirability of automatically switching or controlling the capacitors.

On some utility and industrial system circuits, capacitors have been or are being applied to the extent that it becomes desirable to switch part of them at time of light load to control voltage and to minimize circuit losses.

Recent studies[1] have shown that as systems grow, capacitors on distribution feeders and in substation banks have a definite place in the growth process. There should be, therefore, an increasing demand for automatic control equipment in this connection. Where industrial plants are encouraged, by means of the power-factor clause in the utility rate structure, to supply part of their own magnetizing kilovars, it sometimes may be desirable to switch the capacitors. This paper offers application information as a guide to the selection of proper automatic switching equipment, and describes available types of control equipment.

Reasons for Switching Capacitors Automatically

Capacitors applied for voltage-regulation purposes should be automatically controlled where manual switching is not convenient or reliable. Switching also may be necessary or desirable to avoid overvoltage or excessive losses, particularly during light-load periods for capacitors used to reduce circuit loadings, or to meet the requirements of the power-factor clause of an electric-power rate.

Switching usually need not be considered if the minimum-load kilovars exceed the capacitor kilovolt-amperes. It is shown in the appendix that circuit losses will be a minimum if the unswitched capacitor kilovolt-amperes equals the average kilovar requirement of the load, that is, the average ordinate of the daily kilovar-load pattern. In some cases automatic switching equipment cannot be justified economically if losses are the only consideration.

Types of Equipment

In addition to the capacitors, automatically controlled capacitor equipment consists of:

A. Switching device. For convenience, this may be divided into the following classes of application:

1. Low-voltage circuits (600 volts or less)
2. Medium-voltage circuits (2,400 to 13,800 volts)
3. Utility distribution feeder circuits.

B. Control equipment for automatically controlling the switching device associated with the block of capacitors.

Main Switching Device

1. Switching Equipment for Low-Voltage Circuits. This voltage class (up to 600 volts) is encountered most generally in industrial plants, with the nominal 460-volt circuit being most common. The two types of equipments for this service are air circuit breakers and contactors.

Standard air circuit breakers generally are used, and have the advantage of providing fault protection in addition to the switching function. Breakers are not designed for highly repetitive operation, but in the average industrial plant the number of operations is not large enough to result in a serious maintenance problem. In those installations where the number of operations is large, contactors are the logical selection and are available with or without fuses.

2. Switching Equipment for Medium-Voltage Circuits. Standard power circuit breakers are used almost always as the switching device for power circuits in the voltage range of 2,400 to 13,800 volts. Contactors are available for 2,400- and 4,160-volt service.

3. Switching Equipment for Use With Utility Distribution Circuits. This class of switching equipment is limited almost entirely to the utility field for use with small banks of capacitors in which the switching device is primarily a switch only and not a breaker; fault protection is provided by other means. Here a power breaker could not be justified economically for the small capacitor banks (45 to 180 kva) located on distribution circuits. Oil switches for pole mounting are available for this service in ratings up to 100 amperes and 5,000 volts. A typical unit-type equipment, including capacitors, switch, and control for a 180-kva bank is shown in Figure 1.

Automatic Control Equipment

The control is the heart of the equipment; it receives the signal, interprets it, and then sets into operation the proper devices to perform the desired function. This control consists fundamentally of the following:

1. Master Element. This is the "brain," and may be a voltage-, current-, kilovar-, or power-factor-sensitive device.

2. Time-Delay Device. A time-delay device is a practical necessity to avoid unnecessary operation of the switching device for signal changes that are only momentary. This may be a separate device or may be incorporated as part of the master element.

3. Sequencing Device. This is necessary only for multistep control, and determines the order in which the capacitor groups are switched in and out of service.



Figure 1. Unit-type pole- or base-mounted automatically controlled capacitor equipment, 180 kva, 2,400 volts

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4. Miscellaneous Relays or Devices. A lockout device is required to prevent the switching device from reclosing on a fault. It is also common to include, for convenience of control, an "automatic–manual" transfer switch and a "close–trip" switch for each capacitor block.

Control power is standardized at 220 volts alternating current or 125 volts direct current, following switchgear and control practice.

The simplified control schemes for the two most common controls are shown in Figures 2 and 3. To illustrate the function of each of the component parts, the detailed performance of a typical control will be outlined. The sequence for a single-step voltage-sensitive control is quite straightforward and will not be discussed. It should be noted that, in accordance with standard practice, the devices are shown in their de-energized position.

Figure 3 shows the schematic diagram and Figure 4 a control panel for a three-step kilovar control. The sequence of operation is as follows: As the load magnetizing kilovars increase and reach a predetermined value, the master element operates, initiating the time-delay device and establishing a closing circuit for the correct direction of the sequencing switch. If, after a preset time delay, the master element still indicates capacitors are needed (that is, the signal was not the result of just a momentary disturbance), the time-delay device operates and starts the sequencing device. Once the sequencing device is started its motor continues to operate until the first position is reached, when a set of contacts closes to initiate the closing circuit of the switching device. The same control-sequence procedure is followed for switching in successive blocks. When the load kilovar requirements decrease, the capacitors are switched out of service in the opposite sequence, that is, the last capacitor group switched into service is the first group switched out of service.

If the signal to the master element does not last for the full time-delay period, the control is arranged so the timing device will be reset for a full timing period for its next operation.

Master Control Elements

There are two characteristics common to all regulating types of master elements that should be fully understood to select intelligently the proper device. These are the range of adjustment and the band width.

The range of adjustment refers to the normal operating level of the device, and is necessary, for practical purposes, to allow for differences in operating conditions. For example, the system voltage level may vary over a rather broad range so the normal setting of a voltage-sensitive device must be capable of adjustment.

Band width is defined as the difference between pickup (switching a capacitor on) and dropout (switching a capacitor off). *The band width must be larger than the change in signal caused by switching the largest block of capacitors to prevent pumping action of the control and switching equipment.*

The most common types of master-control elements are:

(*a*). Voltage.
(*b*). Current.
(*c*). Kilovar.
(*d*). Power factor.
(*e*). Time switch.

(*a*). *Voltage-Sensitive Device.* This is essentially a contact-making voltmeter; the most common type is that used with voltage regulators. Standard voltage-sensitive devices are available with a range of adjustment from 90 to 110 per cent, and with a band width adjustable from three to seven and a half per cent. A multistep control requires the lower band width, as it is likely to be used for a regulating duty where fine control is necessary.

Table I. Application of Automatic Capacitor Switching Control

Type of Master Element	Switching Function: Removal of Capacitors at Light Load	Regulating Voltage	Regulating Kilovars	Switching Capacitors on Basis of Power-Factor Clause Requirements* Power Factor for Billing Determined By: Average Monthly Power Factor: Occasional Measurements	Average Monthly Power Factor: Continuous Measurements	Average Monthly Power Factor: Kwhr and Kvarhr Comparison	Power Factor at Maximum Kw Demand: Occasional Measurement**	Power Factor at Maximum Kw Demand: Continuous Measurement of Power Factor	Power Factor at Maximum Kw Demand: Kwhr and Kvarhr Comparison	Billing on Kw and Kvar Demand	Billing on Kva Demand
Voltage, single-step	O.K.										
Voltage, multi-step	Usually not necessary	O.K.									
Current, single-step	O.K. if power factor versus load characteristic known										
Current, multi-step											
Kilovar, single-step	O.K. where voltage magnitude not satisfactory signal			O.K.	Leave capacitors unswitched	Leave capacitors unswitched	O.K.	O.K.	O.K.	O.K.	O.K.
Kilovar, multi-step	Usually not necessary	O.K.		O.K. where graded control desired			O.K. where graded control desired →				
Power factor, single-step				O.K. but desensitizing control required			O.K. but desensitizing control required →				
Power factor, multistep				O.K. where graded control desired but desensitizing control required			O.K. where graded control desired but desensitizing control required				

* Where capacitors are used for rate purposes only, they need not be switched unless there is a penalty for leading power factor. It may be desirable to switch capacitors at reduced loads to prevent excess voltage and losses

** At or near maximum load.

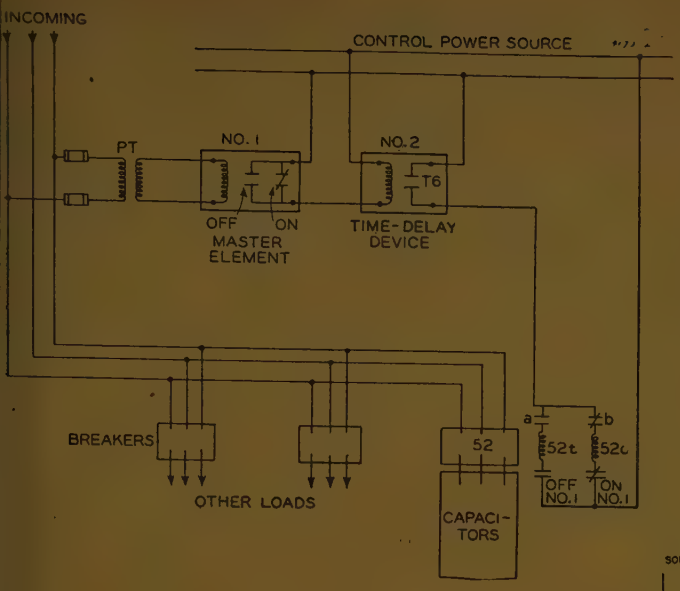


Figure 2. Simplified schematic connection diagram showing single-step voltage control

Devices:
1—Master element
2—Time-delay device
52—Capacitor switching device (breaker)
PT—Potential transformer

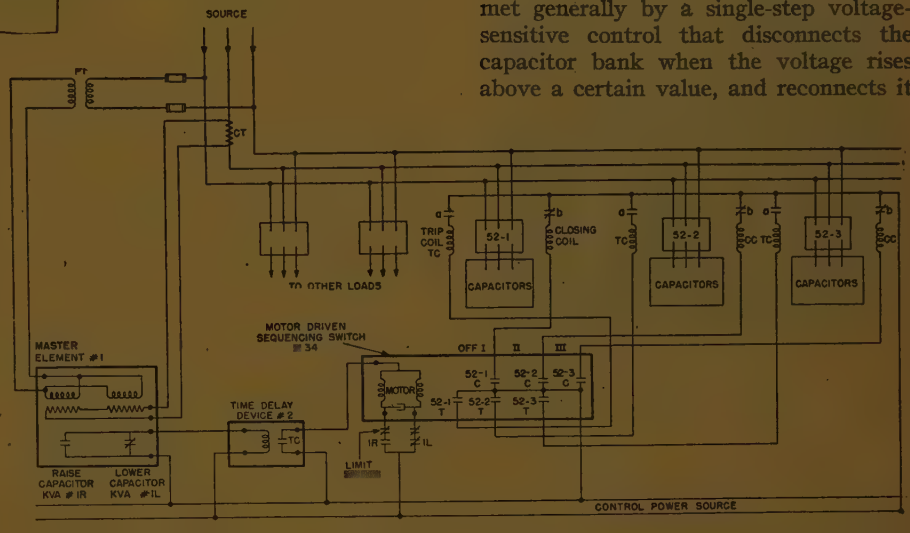
(*b*). *Current-Sensitive Device.* This is essentially a contact-making ammeter, and since this type of control is more likely to be for an ON–OFF than for a regulating function, it usually is furnished in a single-step control. The standard range of adjustment is 25 to 100 per cent of the coil rating (five amperes).

(*c*). *Kilovar-Sensitive Device.* A kilovar-sensitive device may be thought of as a contact-making wattmeter connected to read kilovars; usually it is an induction-type directional relay for single-phase indication. It is customary to use two master elements for multistep control in order to provide a simple and convenient means for adjusting the band width. One master element can be used for single-step control, and a suitable band width can be obtained by a recalibrating device.

The characteristics of the kilovar or reactive-ampere master elements commonly are given in terms of reactive amperes referred to the secondary side of the current transformer (five amperes normal). A useful range of adjustment is 3.3 amperes (66 per cent) lagging to 1.6 amperes (32 per cent) leading, with a band width adjustable from 2.5 to 0.7 amperes (50 to 14 per cent).

(*d*). *Power-Factor Device.* Since power factor is the measure of an angle, the usual power-factor or angle-measuring devices are suitable for this purpose. What has been said regarding the kilovar-sensitive master element also applies generally to a power-factor-sensitive master element. It is usually an induction type directional relay.

Since the band width in *kilovars* of a power-factor control is variable and changes uniformly with kilowatt load, and because at light load the band width may be smaller than the change in signal caused by switching the largest block of capacitors, some form of desensitizing control is necessary to prevent a pumping action at light loads.



(*e*). *Time-Switch Device.* This is a simple control device used for switching capacitors at a predetermined time, and is used on some utility feeder circuits where the load characteristics with time are reasonably constant.

Time-Delay Device

A time-delay device may consist of an electronic or motor-driven timer. Usually, only one time-delay device is used, regardless of the number of steps. If, however, a different time-delay is required for the two sequences, two devices are required. The amount of time-delay is not critical, and is set long enough to avoid unnecessary operation because of momentary circuit disturbances. Range of adjustment is desirable; accuracy in timing is not of extreme importance. Practical experience has indicated that the range of adjustment is from 5 to 20 seconds.

Sequencing Device

A sequencing device is used only for multistep control, and consists of an arrangement of interlocked relays or a motor-driven cam switch.

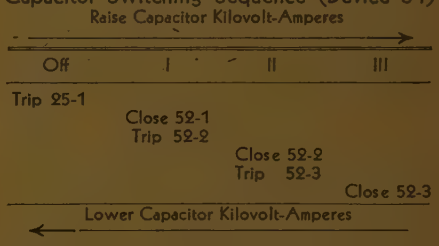
The interlocked-relay scheme is simple, and can be used for two-step control. For a larger number of steps, the motor-driven sequencing switch is simpler and less expensive, and also has the advantage that additional steps of control may be obtained conveniently by adding additional contact positions on the switch. With the interlocked-relay scheme, a major change in wiring and arrangement is required to increase the number of steps.

General Application of Automatic Control Equipment*

For Removal of Capacitors at Light Loads to Reduce Voltage and Losses

In many cases this will be the only switching requirement, and it may be met generally by a single-step voltage-sensitive control that disconnects the capacitor bank when the voltage rises above a certain value, and reconnects it

Figure 3. Simplified schematic connection diagram showing three-step kilovar control



when the voltage falls below a predetermined value.

If the supply-system voltage is subject to variations independent of variations in the load with which the capacitor bank is associated, the voltage-sensitive control may cause the bank to be switched somewhat independently of the load. For example, in some cases the capacitors may be switched off during peak load, or, switched on during light load, with resultant feeder overload or undue losses. This can be obviated by the use of a kilovar or, in some cases, a current-sensitive control.

The inherent characteristic of the

* A discussion of the application suggestions summarized in Table I.

power-factor-sensitive control requiring it to be desensitized at light load to prevent hunting precludes its use for the application considered in this section.

For Voltage Control

If voltage control is the primary objective, a voltage-sensitive control will be



Figure 4. Control panel for three-step kilovar control

used, and since voltage control usually implies holding the voltage within rather close limits, a multistep control should be used. The capacitor kilovolt-amperes per step should be co-ordinated properly with the voltage band width.

For Kilovar or Power-Factor Control*

This application is concerned with bulk kilovar control in connection with kilowatt flow past a purchase or interchange power point, or with maintaining a high power factor to minimize kilowatt losses. The master elements to be considered are kilovar or power-factor-sensitive devices.

In power-dispatching work, the two-quantity concept, which treats kilowatts and kilovars as two separate and independent quantities, is gaining increasingly greater favor. In instances where this concept is adhered to, switching the capacitors as a function of kilovars doubtless will be the predominant approach.

To compare the relative merits of a kilovar-sensitive and a power-factor-sensitive master element where the prime objective is to control the flow of kilovars to a large load, refer to Figure 5. Part (a) shows the circuit involved, with a multistep capacitor bank installed near the load for supplying the load kilovars to maintain a peak load power factor of 95 per cent.

The total kilovolt-amperes of capacitors needed is dictated by the maximum requirements of the load. The number and size of steps in which these capacitors are switched is governed by the fineness of control desired. Unless the kilovar versus the kilowatt component of load (the line *a–b* of Figures 5b and 5c) is known to be fairly constant at all times, it probably will be best to switch the capacitors in uniform steps to prevent the possibility of hunting.

Let the line *o–a* represent the minimum uncorrected load and *o–b* the maximum uncorrected load. Assume line *a–b* to represent the uncorrected-load variation between these two extremes. The line *b–c* then represents the total capacitor kilovolt-amperes applied at maximum load to obtain the desired corrected-load power factor, in this case

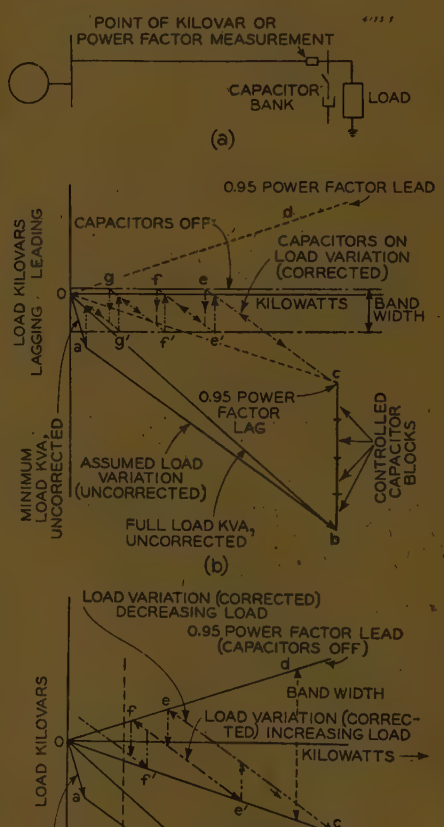


Figure 5. Comparison of multistep kilovar- and power-factor-sensitive control used to switch capacitors automatically for regulating kilovars

(a). One-line circuit diagram
(b). Performance diagram with kilovar control
(c). Performance diagram with power-factor control

95 per cent lagging. The corrected-load characteristics are on the basis of four equal blocks of capacitors.

Since the kilovar band width of a power-factor-sensitive control is variable from zero at no load to maximum at full load, it is necessary that the automatic control be made inoperative for loads below which the band width in kilovars is smaller than the capacitor block to be switched. This point is designated by the line labelled "cut off" in Figure 5c. At loads below this, the remaining capacitor blocks are switched manually.

Setting the kilovar-sensitive control in this manner permits the power factor of the corrected load to fall outside the 95 per cent band at light loads; but this is not important in this case, since the basic requirement is to control kilovars. However, the load at which the power factor falls outside the 95 per cent band may be reduced considerably by raising the operating band of the control vertically and sacrificing the very low leading kilovar characteristic. This load can be reduced still further by use of smaller switched blocks.

It will be noted from the uncorrected-load characteristic and the size of capacitor blocks shown in Figure 5c that, if a power-factor control is used, the automatic switching will be done on only two steps instead of the desired four steps as shown. If, for this reason, switching in fewer and larger steps is considered, the "cut off" point will occur at a higher load; this will mean a smaller operating region in which the automatic control is effective.

It can be seen from a comparison of Figures 5b and 5c that a kilovar-sensitive control set for low lagging kilovars and very low leading kilovars will provide a more constant value of kilovars at all loads than does the power-factor-sensitive control. The low leading kilovar feature may be of particular value in this case if it is not desirable to have kilovars flow from load to the power source. A power-factor control entails complications if power flow may be in either direction past the control point.

For Meeting the Requirements of the Power-Factor Clause of an Electric-Power Rate

If capacitors are applied for rate purposes only, and voltage rise and losses at light load are not a problem, it is not necessary to switch the capacitors unless there is a penalty for leading power factor.

If load power factor is determined on an average monthly basis by continuous measurement or by an integration of the kilowatts and the kilovars for the month, it will be advantageous to the consumer to leave his capacitors connected at all times if possible. Otherwise, generally speaking, they can be off except during the period of maximum demand, or during

* Exclusive of that for rate-clause purposes.

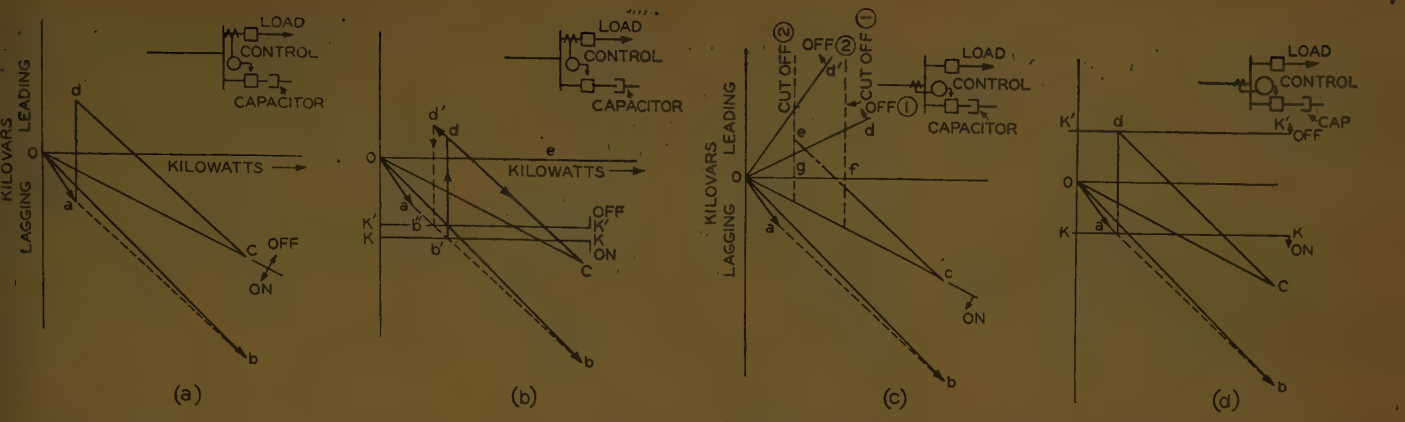


Figure 6. Comparison of single-step kilovar- and power-factor-sensitive control used to switch capacitors applied on behalf of rates

(a). Single-step power-factor control measuring uncorrected load

(b). Single-step kilovar control measuring uncorrected load

(c). Single-step power-factor control measuring corrected load

(d). Single-step kilovar control measuring corrected load

a period of relatively heavy load (not necessarily maximum demand) when the "occasional" measurement is apt to be made.

Where it is desired to have the capacitors on at heavy loads for rate reasons, but off at light load to minimize voltage rise or losses, the kilovar or power-factor master element will provide the most direct solution of the control problem. Unless multistep switching and control to regulate voltage or to keep the losses at a minimum can be justified, a single-step control, judiciously set, will be sufficient.

Figure 6 shows a comparison of the single-step kilovar and power-factor control used primarily in conjunction with a rate clause. The diagrams plot uncorrected and corrected kilovars versus load kilowatts. In all cases a light-load kilovolt-ampere *o–a* and a maximum-load kilovolt-ampere *o–b* is considered, with a straight-line variation between these limits for simplicity. Assume it is desired to hold a power factor at peak load as represented by the line *o–c*. A single-step power-factor control viewing the *uncorrected-load power factor*, as in Figure 6a, switches the capacitors on for load power factors below *o–c* and switches them off for load power factors above *o–c*. (Actually, to avoid unnecessary operation, this type of control would have a band width of a few degrees, but this is neglected in this analysis.) For the load pattern shown, the capacitors are on even at light load, with a resultant leading kilovar component corresponding to point *d*. As the locus of the uncorrected-load kilovolt-amperes proceeds along the assumed pattern *a–b*, the locus of the corrected pattern follows *d–c*. Therefore, for this load pattern, the capacitors are never off and the power factor is unnecessarily high at light load.

If the power-factor control measures the *corrected power factor* as shown in Figure 6c, the ON and OFF characteristics would result in a band width varying with the kilowatt load. However, an auxiliary control device operable at point *d'* is necessary to prevent the possibility of hunting. The load range of operation of the control could be increased somewhat by moving the OFF characteristic to *d'*, but this scheme has the disadvantage of the complication interposed by the desensitizing relay and its adjustment.

The kilovar control element measuring *uncorrected kilovars*, Figure 6b, has an operating characteristic *kk* and *k'k'* (OFF and ON again differing practically by a small band width). The relay pickup is set somewhat above the corrected kilovar component of the expected peak load to assure the capacitors being on at heavy loads which are not necessarily up to the expected peak. As the uncorrected load increases, the capacitors go on at *b'*, making the corrected load *o–d*. At peak load the corrected kilovolt-amperes are *o–c* and, as the uncorrected load decreases to *o–b''*, the capacitors are switched off.

If the single-step kilovar control is connected to view *corrected kilovars*, the performance is as shown in Figure 6d. Note that the band width must be widened to exceed slightly the capacitor-bank kilovolt-amperes.

It has been pointed out previously that if multistep switching is used for rate purposes, it probably will be for voltage regulation and to minimize losses. With this objective, it is considered that multistep kilovar control will provide the best results more simply than a power-factor-sensitive control as may be appreciated from a study of Figure 5.

Factors Influencing Operation of Control Equipment

Size and Number of Capacitor Groups

The type of automatic control for capacitor switching is independent of the kilovolt-amperes and voltage rating of capacitor blocks. The rating of a block and the number of blocks in a bank is determined by load requirements and the fineness of control desired. It usually is best to switch in blocks of equal rating, unless the daily kilowatt-versus-kilovar pattern of the load is definitely known and known to be the same from day to day.

Point of Signal Measurement

Voltage Control. When automatic capacitor switching is done in response to voltage, the master-control element should obtain its signal from the point at which voltage is to be controlled. Where capacitors are located on feeders supplied from a common bus, with the bus in turn supplied from a system of limited capacity such that switching the feeder capacitors causes appreciable voltage change at the bus, the signal for switching should be taken from the bus. If individual feeder voltage control is attempted, the bus-voltage variation as a result of switching one of the feeder capacitors may require the switching of other feeder capacitors, with a resultant hunting action of the controls.

Current Control. The single-step current control may measure the uncorrected-load current for simplicity. The multiple-step current control should receive its signal from the uncorrected-load current to prevent false operation, and this requires a separate master element for each step, or a recalibration for each step if only one master element is used.

Kilovar or Power-Factor Control. Individual cases will determine whether corrected or uncorrected load should be used as a signal for single-step control (see Figure 6). A multistep control should measure the corrected load to assure proper operation, regardless of possible variation in load characteristics.

Desensitizing Requirement of Power-Factor Control

It has been shown previously that in most cases the power factor type of control should be made inoperative at low load to prevent the possibility of hunting action. To preclude all possibility of such hunting, the "cut off" or desensitizing operation should occur at some predetermined value of kilowatts; thus the need for a contact-making wattmeter

in addition to the power-factor control is indicated. In some cases it is possible that a current relay can be used to provide "cut off" with reasonable accuracy and little danger of hunting or pumping action of the power-factor control.

Appendix. Determination of Optimum Unswitched Capacitor Kilovolt-Amperes to Give Minimum Circuit Losses

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Figure 7 shows the variable portion of the daily kilovar load pattern of a circuit. Let I_c represent the unswitched capacitor

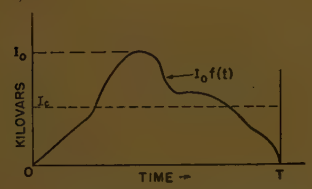


Figure 7. Daily kilovar load pattern of a circuit

kilovars or current. The square of the rms corrected current in the circuit is given by

$$(I_{rms})^2 = \frac{1}{T}\int_0^T \{I_0 f(t) - I_c\}^2 dt =$$

$$I_c^2 + \frac{1}{T}\int_0^T \{I_0 f(t)\}^2 dt - \frac{2}{T} I_c I_0 \int_0^T f(t)\,dt \qquad (1)$$

Differentiate equation 1 with respect to I_c, equate to zero, and solve for value of I_c to minimize the loss, and obtain

$$\frac{d(I_{rms})^2}{dI_c} = 2I_0 - \frac{2I_0}{T}\int_0^T f(t)\,dt = 0 \qquad (2)$$

or

$$I_c = \frac{I_0}{T}\int_0^T f(t)\,dt \qquad (3)$$

$I_0 \int_0^T f(t)\,dt$ = area of the load pattern,

and when divided by T minus the width, the average ordinate is obtained. Hence, for *any* kilovar pattern, the minimum circuit loss with a fixed kilovar supply is obtained by using a value of I_c equal to the *average* value of the requirement.

Reference

1. THE EFFECT OF KILOVAR SUPPLY ON THE DESIGN OF SYSTEMS FOR LOAD GROWTH, T. W. Schroeder, J. W. Butler, N. H. Meyers. ELECTRICAL ENGINEERING, volume 63, February 1944, pages 69–76.

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History and Development of the Electronic Power Converter

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THE TERM "electronic power converter" needs some definition. The object may be to convert power from direct current to alternating current for d-c power transmission, or to convert power from one frequency into another, or to serve as a commutator for operating an a-c motor at variable speed, or for transforming high-voltage direct current into low-voltage direct current. Other objectives may be mentioned. It is thus evidently not the objective but the means which characterizes the electronic power converter. Other names have been used tentatively but have not been accepted. The emphasis is on electronic means and the term is limited to conversion of power as distinguished from electric energy for purposes of communication. Thus the name is a definition.

History

Within the scope of the definition a mercury-arc rectifier is an electronic power converter. From this point of view we must trace its history back more than 40 years. In the first 20 years of this period the development was rather slow. An important discovery however was made by Langmuir in 1913 when he found that a negative electric potential introduced between the cathode and anode prevented the start of current flow through the anode.

At this time the initial development took place of the high-vacuum radio tube which was an evolution of the deForest audion. These tubes were soon used successfully at high voltages to convert direct current into alternating current of audio and radio frequencies and serious thought was given to their use for power conversion. The high internal voltage drop was however a serious obstacle. Attention therefore returned to the mercury arc and ten years after Langmuir's discovery, tests were made which proved that the mercury-arc rectifier could be inverted so as to convert direct current into alternating current. The tests were made with a standard high-voltage rectifier of the type used for series arc lighting. It had a bulbous head and two upturned side arms. Carbon electrodes were introduced at the elbows of the side arms and connected to the control circuit. Direct current power of 9,000 volts was in this way successfully converted into alternating current and then again into low-voltage direct current. A new electrical industry was immediately foreseen where power would be transmitted long distances by high-voltage direct current and railroads would be electrified by locomotives carrying electronic converters from a high-voltage d-c trolley to low-voltage d-c motors. After another 20 years this new industry is now well under way on a modest scale but comprising installations up to 20,000 kw.

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Evolution of the Electronic Converter

The first tests of the inverter were made with a mercury-pool tube and the latest installations have mercury-pool cathodes. Between these points there is, however, a significant development. The structure of the tube itself can assume a number of successful forms, such as the thyratron, the ignitron, and the excitron. But all these forms of tubes have certain characteristics in common which force the electrical engineer to accept a new point of view in the methods of its application. This has perhaps been the reason why the practical development has up to the present been rather slow. There has been an unwillingness to face the facts or what so far appear to be the facts, that high-voltage tubes are subject to momentary faults known as arc backs and loss of grid control. The hope that the tubes could be so improved that these faults could be eliminated has so far been wishful thinking. But a much more constructive point of view has been adopted which accepts the electronic faults as inevitable while measures are taken which render the faults perfectly harmless so long as the incidence of the faults is kept within reason. Installations of d-c power transmission and electronic frequency changers are at present operated on this principle with great success. Electronic

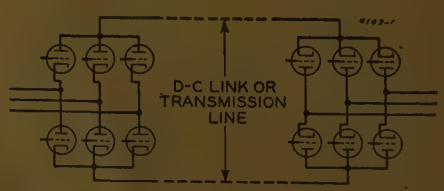


Figure 1. Electronic converter, dual-conversion type

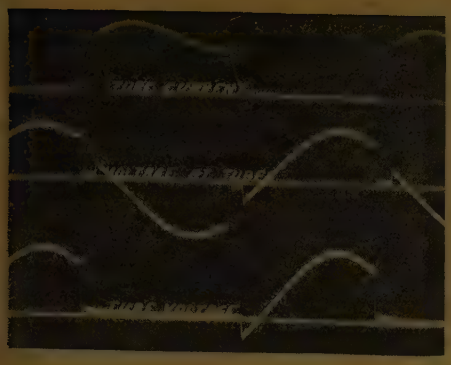


Figure 2a. Wave shapes of single-phase inverter



Figure 2b. Wave shape of six-phase inverter with low a-c reactance

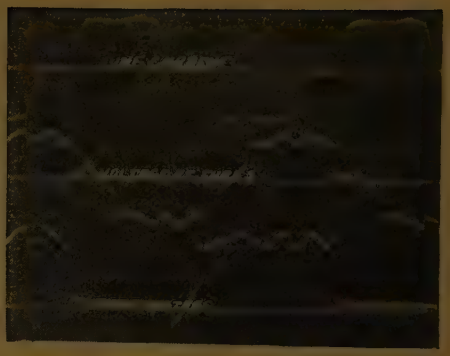


Figure 2c. Wave shape of six-phase inverter with high a-c reactance

faults occur several times a day. They can, however, be observed only by delicate instruments and do not in any way interfere with power service.

Suppression of Electronic Faults

An electronic fault may or may not result in a short circuit, depending upon what means are used to suppress it. In low-voltage rectifier installations the faults occur so seldom that it has proved satisfactory to disconnect the fault by a high-speed circuit breaker. In high-voltage installations the faults occur so often that this would not be permissible because power delivery must be continuous. In the original installation of d-c power transmission for the New York Power and Light Corporation between Mechanicville and Schenectady, this situation was first met by the use of constant current as described in a paper in 1935 by Willis, Bedford, and Elder. In a constant-current system a momentary short circuit is of no significance. Such a system is therefore adapted to the characteristics of the electronic converter. Operation at constant current was successfully continued for four years but when plans were made for future installations it was found that the constant-current system was not well adapted to existing practice of power distribution. The reactance–capacity networks used for conversion between constant current and constant potential were therefore removed and the system has been operating at constant potential for several years. In this connection new means were developed for suppressing the electronic faults. These methods have proved entirely successful and have been adopted in later installations. The sequence of events is illustrated by oscillograms taken during practical operation. Samples of these oscillograms are shown herewith.

Figure 1 shows a simplified diagram of an electronic converter of this type used for d-c transmission and frequency conversion. It should be noted that the power circuits of the rectifier and the inverter are identical and that it is the grid control which makes one part of the whole into a rectifier and the other into an inverter. By change of the grid controls, the power flow can be reversed. The rectifier and the inverter are however not independent devices like a generator and a motor but function together as a whole —the electronic converter. Electronic faults which occur in the inverter are suppressed by the rectifier. The rectifier suppresses its own faults but even this statement must be accepted with some qualifications. The rectifier can suppress its own faults because the inverter cannot feed current back into the rectifier but if several rectifiers feed the same circuit in multiple they can feed current into each other's faults and therefore fault suppression depends upon interaction between the several rectifiers.

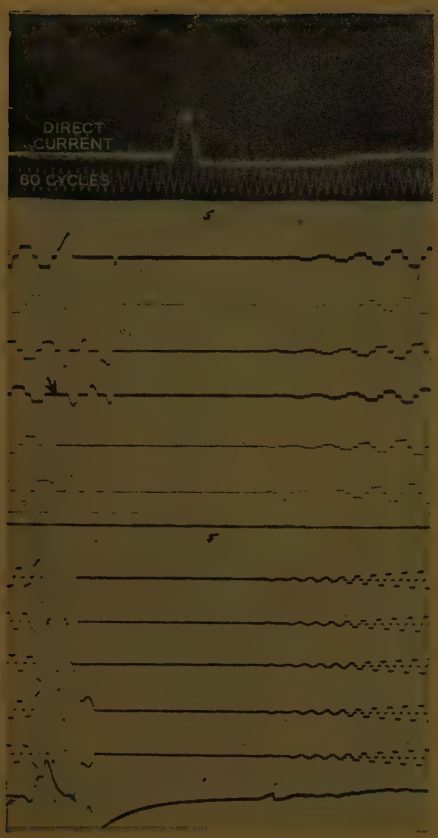
The oscillograms of Figure 2 show a typical performance of a single-phase and a six-phase inverter. The rather complicated wave shapes of the six-phase inverter can be easier understood if the records of the single-phase inverter are examined first. The three recorded curves represent the wave shapes of the current in the a-c line, the voltage between a-c lines, and the voltage between cathode and anode of one tube. The commutation period is marked by the abrupt depression of the alternating voltage near the end of each half cycle. The brief recovery of the voltage before it passes through zero is the margin of commutation. This same margin is also indicated by the brief negative swing of the voltage between cathode and anode. If the load is increased to the point where this margin is used up, the inverter goes into short circuit. With this in mind we can examine the six-phase wave shapes and interpret them as the composite of three single-phase wave shapes. Each phase wave shows its own commutation period and also secondary reactions from the commutation of the other phases.

The electronic fault may be of the nature of an arc back or a loss of grid control known as a "shoot-through." A simple arc-back has so little effect upon the operation of an inverter that it is difficult to discover whether it has taken place. It wipes out the negative voltage that marks the commutation margin but this in itself causes no disturbance. If, on the other hand, the ionization left over from the arc back causes the grid to lose control in the next half cycle the result is a shoot-through. This occurrence is, however, more the exception than the rule. It seems to be one of those sequences of events characteristic of electronic phenomena which follow a law of chance over which we have no control. But as engineers, we are not much disturbed about this so long as we can avoid any harmful consequences.

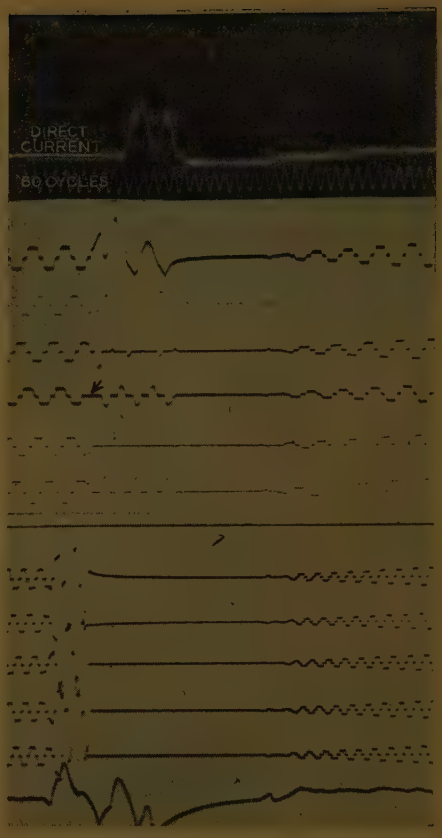
Besides arc backs there are two other causes which may initiate shoot-throughs in an inverter. These are not electronic faults in the same sense as arc backs, and there is a fair chance that they may be entirely eliminated. One of these causes which has been observed in our earliest installations is voltage transients of very short duration which make the grid momentarily positive at a time when it should be negative. Faults from this cause have been eliminated by the use of suitable filters. A third cause of faults is the occasional failure of an igniter to fire. This difficulty has not yet been entirely overcome but the rate of progress in this technique promises that it will eventually be entirely eliminated.

While thus some causes for faults are gradually being removed we must face the conclusion that shoot-throughs in high-voltage inverters will occur from time to time and we must be prepared to meet this situation in a practical way.

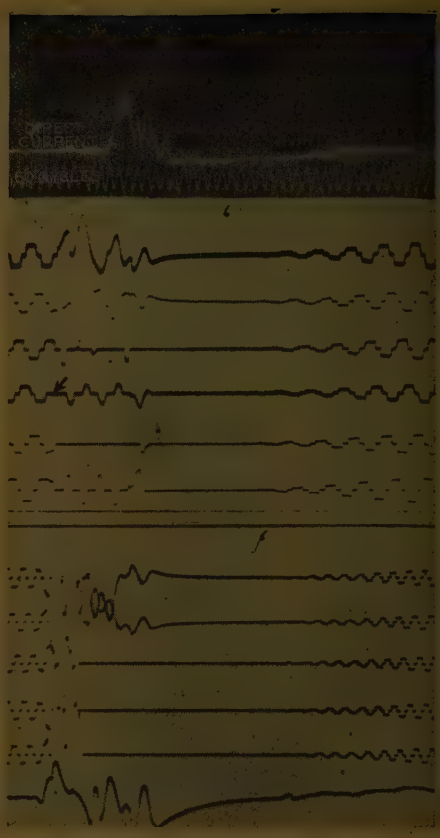
From this starting point there are several possibilities. The inverter may be adjusted so as to work with a good margin of commutation and the power supply may have a short-circuit current that does not greatly exceed the normal load current. In that case the inverter may have enough margin to commutate the increased current and the fault clears itself. It is then difficult to discover that it has occurred and the operator would seldom notice it. Faults which clear themselves are characteristic of circuits containing several rectifier and inverter units in series. It was the normal performance of a 24-phase developmental set tested a number of years ago with four rectifier units and four inverter units in series. This is worthy of note



A. Fault involving opposite inverter tubes in both inverters. Both coils of d-c reactor in. Bottom trace is secondary current of current transformer in direct current

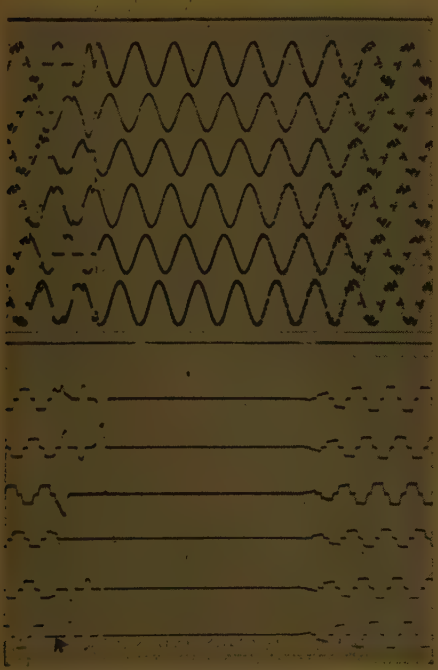


B. Fault including two legs of one inverter transformer. Both coils of d-c reactor in. Bottom trace is secondary current of current transformer in direct current



C. Fault including two legs of one inverter transformer and two legs of one rectifier transformer. Both coils of d-c reactor in. Bottom trace is secondary current of current transformer in direct current

Figure 3. Oscillograms of inverter faults. Arrows indicate made fault—igniter miss



D. Wave shapes of phase voltages and phase currents during faults

when we contemplate d-c power transmission at high voltages. Such installations will necessarily have many units in series and they can be so designed that electronic faults will clear themselves. Each unit in such a system operates at constant potential but an electronic fault in one tube does not disturb the sum of electromotive forces in the system enough to interfere with normal operation.

Returning to the sequence of events that may follow an electronic fault we may assume that in some cases the current which follows the fault is so high that the inverter cannot commutate it. This is the case in the d-c power transmission from Mechanicville to Schenectady and in the frequency changer described in companion papers. In these cases it is necessary to clear the fault by control of the rectifier. The oscillograms of Figure 3 show such a typical sequence. The sudden rise in current acts upon a fast relay controlling the igniter and grid circuit. Consequently the current which has been built up in the power circuit is no longer maintained by any electromotive force and decays at a rate dependent on the time constant of the circuit. It is important that the rectifier should not go into action again until the current has decayed to a value which the inverter can commutate. A time relay delays the action accordingly. In about one-fifth of a second the normal operation is resumed.

The Single-Conversion Frequency Changer

Another type of electronic converter is shown in Figure 4. It is the frequency changer used with the thyratron motor described in a paper by Alexanderson and Mittag in 1935. This electronic converter has not yet been given a name by which it can be readily identified. It is proposed here that it be known as the single-conversion frequency changer. The energy is converted by electronic means only once whereas in the dual-conversion type of frequency changer the power is first converted into direct current and then back to alternating current. Both function in the same way as a non-synchronous tie between two a-c systems. Besides this the single-conversion type has many desirable characteristics, such as being adapted to feed a stub-end load. The motor for which it was originally developed is a stub-end load and starts from standstill with a good torque like a d-c motor. The single motor may be replaced by a synchronous condenser and an independent distribution system including a multiplicity of motors.

The problem of suppressing electronic faults is solved in this type of electronic converter in a different way. When for one reason or another a loss of grid control occurs in one of the tubes the resulting disturbance lasts only one-half cycle because each tube is a rectifier as well as an inverter and the rectifier function commutates the fault current out of the tube where the fault occurred. The

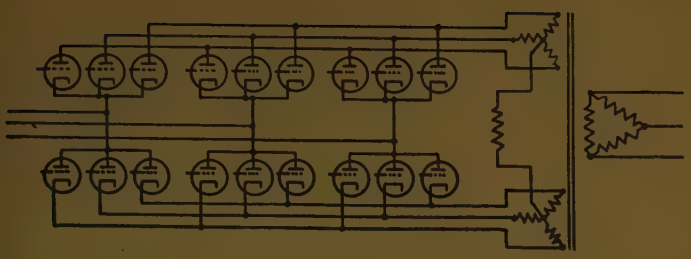
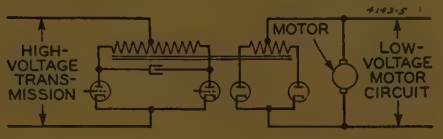


Figure 4 (left). Single-conversion-type frequency changer

Figure 5 (below). D-c transformer



practical result is that this type of electronic converter is entirely free from the problem known as shoot-through in the simple inverter. Thus it will continue to function with slightly reduced power even if several tubes are disconnected, whereas the simple inverter becomes inoperative if a single tube fails to function.

The D-C Transformer

One of the first objectives in the development of the inverter was to provide a way for transforming direct current from high voltage to low voltage. D-c transformation has not yet found application in any large scale. But the experience gained in d-c power transmission indicates that it is from a technical point of view entirely practical. The electronic industry is waiting for a demand where this experience can be applied. The questions that are likely to be asked are: How will it start? Will it require moving machinery? How will it ride through faults?

In order to give a practical answer to these questions, tests have been made represented by the diagram, Figure 5, and oscillograms, Figure 6. The power source is assumed to be a rectifier station like the one used in the d-c transmission from Mechanicville to Schenectady. The receiving station has no moving parts. It has a step-down transformer from the high-voltage inverter to the low-voltage rectifier. A capacitor is in shunt with the transformer. The load is a d-c motor. One oscillogram shows how the operation is initiated from a dead start by application of the grid control to the high-voltage inverter. A second oscillogram shows the characteristic wave shapes.

Adaptability of the Electronic Converter

Three types of electronic converters have so far been discussed and it has been shown how means have been provided for suppressing electronic faults. In practical operation there are also apt to be circuit faults. The d-c transmission line is for instance sometimes struck by lightning. The short circuit following a lightning stroke is cleared by the control system which is provided for suppressing electronic faults. The transmission line is therefore immune to lightning strokes so long as permanent damage is not done to the insulators. In the design of the electronic converter a virtue has thus been made of a necessity and a real improvement in practical reliability has resulted. Disturbances from other causes than lightning may result in circuit faults. Whether such faults can be counted on to clear themselves automatically depends upon the characteristics and stability of the systems that are involved. An electronic converter has a definite load limit like an inductance motor or a synchronous motor. In the grid control we have, however, excellent means for regulating the system so that those load limits are never exceeded. Such regulating systems have been incorporated in the installations now in operation as described in companion papers.

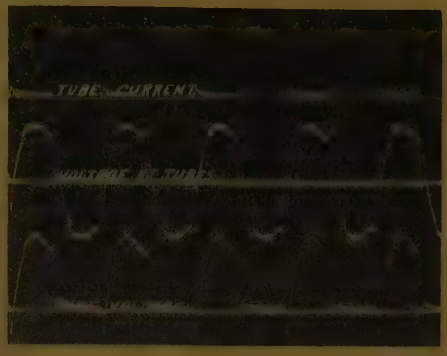


Figure 6a. Wave shapes of inverter in d-c transformer

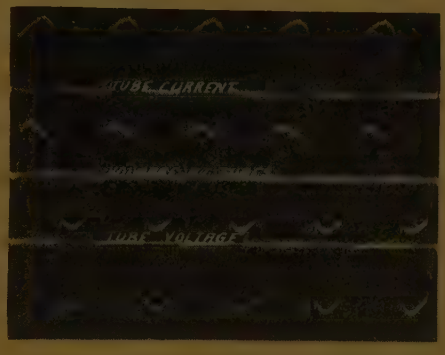


Figure 6b. Wave shapes of rectifier in d-c transformer

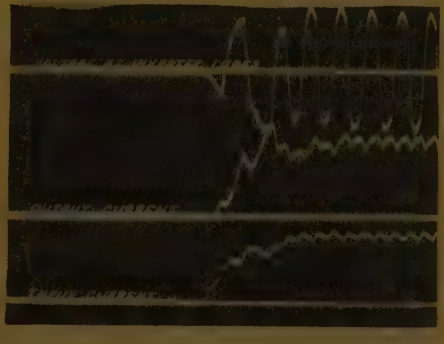


Figure 6c. D-c transformer starting a motor load

It is important that the application engineer should understand the characteristics and limitations of the several types of electronic converter.

The rectifier–inverter combination may be described as a frequency changer or a d-c power transmission depending upon whether they are located in the same station or far apart and joined by a d-c transmission line. The Mechanicville-Schenectady installation is a frequency changer as well as a transmission because the power is generated at 40 cycles and delivered at 60 cycles. Similar installations may fill a demand in the future as nonsynchronous links between 60-cycle systems which are not operated in synchronism. This type of installation is primarily adapted to exchange of power between independent systems. In its original form, it is not adapted to start a dead load. When needed, however, suitable arrangements will undoubtedly be made. An electronic d-c transformer fed from a d-c line may be used to start a dead load as shown on the accompanying oscillograms. The power thus made available may be used to start a synchronous condenser so that it can be fed from another electronic converter. This will serve to illustrate many possibilities of using the electronic power converter in one form or another. It can safely be said that with the installations of the electronic power converter already made, this new tool of the electrical industry has come out of its infancy.

References

1. The Direct-Current Transformer Utilizing Thyraton Tubes, D. C. Prince. *General Electric Review*, volume 31, pages 347–50.

2. Hot-Cathode Thyratons, A. W. Hull. *General Electric Review*, volume 32, number 4, pages 213–23; number 7, pages 390–9.

3. Control of an Arc Discharge by Means of a Grid, A. W. Hull, I. Langmuir. *Proceedings* of the National Academy of Science, March 1929.

4. Grid-Controlled Mercury-Arc Rectifiers, H. D. Brown. *General Electric Review*, volume 35, pages 439–44.

5. Grid-Controlled Rectifiers and Inverters, C. C. Herskind. AIEE Transactions, volume 53, 1934, June section, pages 926–35.

6. Thyratron Motor, E. F. W. Alexanderson, A. H. Mittag. AIEE Transactions, volume 53, 1934, November section, pages 1517–23.

7. Constant-Current D-C Transmission, C. H. Willis, B. D. Bedford, F. R. Elder. AIEE Transactions, volume 54, 1935, January section, pages 102–08.

The Evolution of Standard Lines of Current Transformers for High Overcurrent Capacity

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MEMBER AIEE

Synopsis: Modern switchgear requires, more and more frequently, special current transformers capable of carrying very high fault currents. In the past, the application engineer and the design engineer were not always close together in their work on the current-transformer problem; the application engineer has often had only an approximate idea of what the design engineer could produce, while the design engineer has had only an approximate idea as to general application requirements. The design engineer has not had too sound a basis for many of his designs. His costs were high on account of the multiplicity of special designs.

This paper discusses the basis of design of current transformers for high overcurrents, and describes the co-ordination of mechanical parts with performance requirements to produce lines of current transformers in a definite and easily understood pattern of characteristics. The result is that selection of the proper current transformer is easily accomplished.

The arrangement of the line of mechanical parts and performance characteristics is such that the expense involved in manufacturing these special transformers can be considerably reduced.

IN the early days of switchgear construction, fault currents were not generally as high as they are today. Special current transformers to carry high currents were rarely needed. Today, however, special current-transformer designs for high current capacity have become common. These "special" designs have taken their place in so much switchgear that they have become in fact standard for switchgear. The basis of their design and rating becomes logically a subject of wide interest and importance.

These current transformers have been most confusing to the application engineer. He has understood that the special transformers for high overcurrent had to have, in general, either higher cost or poorer performance, but there was little generally published information to tell him how high the cost or how poor the performance. He was amazed, in some instances, when he found out. The design engineer, on the other hand, has been confused by the wide variation in performance requirements. The resulting multiplicity of special designs has cost too much to design and to manufacture.

The fundamental basis for solution of the problem is logically a design study, but the evolution of standard lines of transformers to fit all conditions is a most important part of the solution for the application engineer. An application study will be described, together with the method of setting up standard lines of current transformers.

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Design Study

The important characteristics of a switchgear current transformer may be listed (given also the insulation class and nominal current ratio)

(*a*). Fault-current capacity[1]
1. Thermal limit (one second).
2. Mechanical limit.

(*b*). Metering performance or accuracy
1. ASA metering accuracy classes.[2]
2. ASA metering burdens.[3]

(*c*). Relaying performance
1. ASA relay accuracy classes.[4]
2. ASA relay burdens.[3]

(*d*). Size and cost.

In general, it is obvious that any characteristic can be somewhat improved at the expense of one of the others. The problem is to get a best compromise of all the characteristics. Let us examine each characteristic separately.

Fault-Current Capacity

1. *Thermal limit.* This limit[1] is defined in a rather complex way which is really equivalent to defining the maximum current density (for one second) as about 90,000 amperes per square inch. To raise the thermal limit, put in more copper.

2. *Mechanical limit.* The study of the mechanical limit divides logically into two parts:

A. Calculation of forces. Various forces act to distort the coils in various ways. (See Figure 1.)

B. Calculation of strength, or the resistance to each form of distortion.

A. Calculation of Forces

The physical nature of the forces in the coils, together with the method used for their solution, is indicated in Figure 1.

Two fundamental principles are available as tools to determine the forces.

$$(a).\quad \text{Force} = F \propto Bli \tag{1}$$

$$(b).\quad \text{Force} = F \propto \frac{i^2}{2}\frac{dL}{dx} \tag{2}$$

The first principle is the more familiar, and is thus more easily used. The second principle can be stated in words, about as follows: the force on an element of a coil, in any direction (taken as x) is proportional to the rate of change of coil inductance with respect to motion of the coil element in that direction. This is a powerful method of attack, since any element or direction of motion may be chosen. The method was demonstrated by Dwight[10] and Clem[5] and used to advantage by Clem in calculating forces in transformers. Both methods are actually used, as expedient.

1. *The expanding force* (which also tends to unwind the outer coil) is most easily calculated according to the assumptions shown in Figure 2. The force

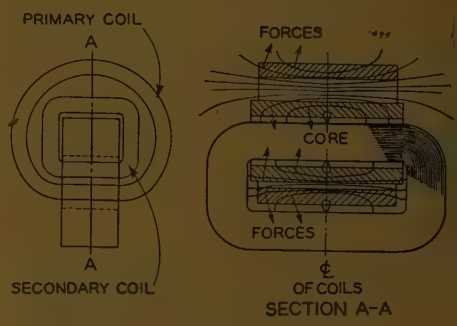


Figure 1. Physical nature of forces

Forces are indicated by the arrows in the right-hand view

In the primary coil, the forces, acting outward, with components toward the vertical centerline tend to expand the outer coil, and to crush it axially

In the secondary coil, the forces tend to crush it against the core

Further inspection of the figure indicates that if the coils are not centered about the centerline of the right-hand view, an axial force tending to slide the coils apart will develop

formula derivation, also outlined in Figure 2, is very simple. This method obviously introduces some error. However, the expanding force is not the important force if the transformers are constructed as shown in Figure 1, and actual failure is very seldom caused by it. Furthermore, even this approximate method gives more accurate results than the best available formula for axial forces, so that further refinement is without point.

2. *Unwinding force:* The principles of Figure 2 may also be used to calculate the force on the end turns of the coil which tend to unwind the coil. If the coil (Figure 4) is N turns thick, the average field strength through the end turn is $2/N$ of the average value throughout the coil itself. The ampere-turns in the outer turn is $1/N$ of the total ampere-turns in the coil. The force per inch of the end turn of a coil constructed according to Figure 4 is then $(2/N)\times(1/N)$ of the expanding force on the coil as a whole, or $2/N^2$.

3. *Axial-displacement force:* The axial forces have been calculated in a number of ways.[5,6] The method developed by Clem[5] has been found to be most accurate and easier to use. The discrepancy between the methods was found to be such that further investigation into the adequacy of Clem's method was carried out experimentally, as follows:

Experimental Determination of dM/dx

The value of dM/dx or rate of change of mutual inductance with displacement, was carefully measured by measuring M for various axial positions of the inner coil relative to the outer. The change in mutual inductance is very small indeed, but can be adequately measured by comparison with standard precision mutual inductances according to the method outlined by Fortescue.[7] This method can easily be used to measure a change in mutual inductance of 0.01 per cent. The coils used were actual current-transformer coils. Figure 3A shows the coils and gives some of the results of the measurements. The change in inductance at one centimeter displacement is only 1.4 per cent, and as the mutual inductance can be measured to not better than 0.01 per cent the measurement of dM/dx is probably not accurate to better than five per cent.

Experimental Determination of Force

The same coils were used to determine the force, according to the set up in Figure 3B. Again the displacement was accurately measurable to about 0.05 centimeter. The difference between the results of Figures 3A and 3B is about eight per cent. The discrepancy can be attributed to experimental error. The agreement is close enough to show the adequacy of the principle that $F \propto (i^2/2)(dM/dx)$.

Calculation of Force by Clem's Method[5]

The formula is (the notation is from the *original* paper)

$$F=0.88I^2a^2nN10^6\times\left[\frac{X_1}{r_1}F_1-\frac{X_2}{r_2}F_2-\frac{X_3}{r_3}F_3+\frac{X_4}{r_4}F_4\right] \quad (3)$$

F = Peak force in pounds
I = Root-mean-square amperes
n, N = Turns in the two coils, per centimeter of axial length
A, a = Effective radii of two coils

$$X_1=X-\frac{S+P}{2} \qquad X_3=X+\frac{S-P}{2}$$

$$X_2=X-\frac{S-P}{2} \qquad X_4=X+\frac{S+P}{2}$$

$$r_1=\sqrt{A^2+X_1^2} \qquad r_3=\sqrt{A^2+X_3^2}$$

$$r_2=\sqrt{A^2+X_2^2} \qquad r_4=\sqrt{A^2+X_4^2}$$

Where X = displacement

$$F_1=1+\left(\frac{a}{r_1}\right)^2\frac{3}{8}\left(\frac{A}{r_1}\right)^2+\left(\frac{a}{r_1}\right)^4\frac{5}{64}\left(\frac{A}{r_1}\right)^2\times\left[7\left(\frac{A}{r_1}\right)^2-4\right]+\left(\frac{a}{r_1}\right)^6\frac{55}{1{,}024}\left(\frac{A}{r_1}\right)^2\times\left[33\left(\frac{A}{r_1}\right)^4-36\left(\frac{A}{r_1}\right)^2+8\right], \text{ etc.} \quad (4)$$

F_2, F_3, and F_4 are similar expressions, functions of r_2, r_3, and r_4 instead of r_1.

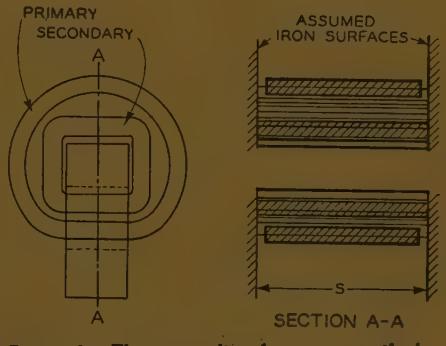


Figure 2. The expanding force can easily be calculated by $F \propto Bli$, if the flux lines are assumed to be straight lines limited at the ends of the coils by magnetic surfaces

The figure shows the lines passing straight across. $B=\frac{0.4\pi NI}{S}$

The force is:

$$F=\frac{11.3(NI)^2}{10^7S}$$

peak pounds per inch of coil perimeter when
NI = root-mean-square symmetrical ampere turns is fully offset
S = inches length of leakage path

The numerical work in an actual problem is considerable, even with the aid of curves and tables, and it is sometimes difficult to get a true answer, because the answer is a small difference between large numbers, each one the result of considerable calculation. Much simplification is possible by the assumption that $S=P$. This is usually true in power transformers, and nearly true enough in current transformers to give satisfactory results.

Then

$$X_2=X=X_3$$

also, for small values of X,

$$r_2=r_3=A$$

and

$$F_2=F_3$$

$$\frac{X_2}{r_2}F_2=\frac{X_3}{r_3}F_3$$

$$X_1\cong-\frac{S+P}{2}\cong-X_4 \text{ and } r_1\cong r_4$$

The corresponding values of a/r and A/r will usually be so small that

$$F_1\cong F_4\cong 1.0$$

Hence

$$\frac{X_1}{r_1}F_1+\frac{X_4}{r_4}F_4=0$$

Returning to equation 3

$$\text{Force}=1.776I^2a^2Nn\frac{X}{r_2}F_2 10^{-6} \quad (5)$$

or

$$\text{Force}=1.776(N_SI_S)^2\left(\frac{a}{S}\right)^2\frac{X}{A}F_2 10^{-6} \quad (6)$$

where N_SI_S = ampere turns (root-mean-square) in coil of axial length S. F_2 is taken from the table in Clem's paper. This formula is obviously much easier to use than the original (equation 3).

Using this formula, and assuming that round coils equivalent to the actual coils in Figure 3 are those which have the same area inside the mean turn, the force comes out to be

$$\text{Force}=0.0614I^2X$$

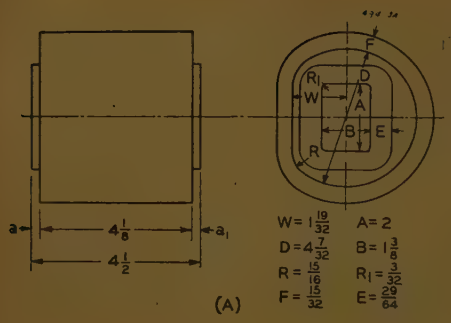
where X is in centimeters. This compares favorably with the actual value of force by either of the other methods (Figure 3)

From measured value of $\frac{dM}{dx}$, $F=0.0867I^2X$ grams; 108 per cent

From measured value of force, $F=0.08I^2X$ grams; 100 per cent

From Clem's method simplified, $F=0.0614I^2X$ grams; 77 per cent

Taking the measured value as correct, the force on the actual transformer coil



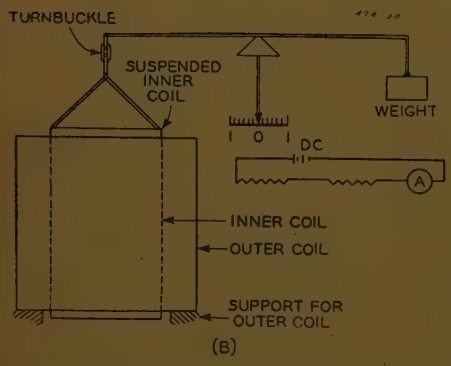


Figure 3. Coils used in experimental determination of dm/dx and F

A—The relative axial position was determined from measurements of a and a_1. The center position ($a = a_1$) did not give quite the maximum value of M. The point at which M was actually maximum was taken as center. If X is displacement from this center (centimeters), the values of M followed very closely the relation:

$$M = 0.00031731 - 0.00000425X^2$$

from which $dm/dx = 0.0000085X$ and force $= 0.0867XI^2$ (grams)

B—Procedure of measurement:

1. Adjust position and weight so that balance is attained with or without current in the coils. This will be when the coils are electrically centered.

2. Without current determine displacement (1.14 centimeters) as additional weight (1 gram) is added.

3. With current (7.8 amperes direct current) determine weight (6.55 grams) to produce same deflection.

4. Force due to current is 5.55 grams.
$F = 0.08 \times I^2X$ (compares with $0.0867\ I^2X$ from Figure 3A).

at normal current ($10\sqrt{2}$ peak amperes) and 0.1 inch (0.254 centimeter) displacement would be

$$F = 0.08 \times 2 \times 100 \times 0.254 = 4 \text{ grams}$$

At 200 times normal current the force would be $200^2 \times 4 = 160{,}000$ grams $= 352$ pounds. This compares favorably with the force of 600 pounds required to separate the coils, described later.

This method is obviously not too exact. However, it is never possible to know the possible displacement in an actual transformer with great accuracy. It is necessary to make some assumption as to maximum possible displacement, which must be assumed large enough to be on the safe side. This automatically provides a fairly large factor of safety, so that great accuracy of calculation is unnecessary. The method given will give a sufficiently accurate comparison of forces in various designs, which is all that is required for the purpose.

B. Calculation of Strength

The resistance to each force can be calculated as follows:

1. *Expanding force.* The expanding force in a coil appears as a tension in the copper which can be calculated in the same way as the tension in the walls of a cyclinder subjected to internal hydraulic pressure.

Given:

F = force per unit of circumference
d = diameter of the coil

Tension in one side $= Fd/2$ and the stress in the copper is $Fd/2$ divided by the total copper cross section. In the coils in question this tensile stress (see Figure 2) is never the critical one; failure always occurs by axial displacement.

2. *Unwinding force.* The end turn is held down in three ways:

By the Micarta terminal brace (Figure 4).
By the treated tape insulation wound tightly over the coil.
By the stiffness of the copper.

Of these, the first and third are difficult to calculate, although they are considerable. The strength of the treated tape is easily calculated, however, and is found to be sufficient to hold the end turn in place, assuming an ultimate tensile strength of 5,000 pounds per square inch for the tape. (For 0.010×1-inch tape, a breaking strength of 50 pounds.)

3. *Arching of straight side of primary coil.* The expanding force tends to arch, or bow, the straight side of the primary coil. The strength of the side is obviously at least that of a beam supported at the ends, uniformly loaded. The beam is made up of the several conductors of the coil, so that it should actually be considered as several separate beams, as many as there are conductors, which is usually equal to the number of turns N. The load can be assumed to be divided equally on each beam. Then, if F is the total force per inch of length of coil l, the load per inch on one conductor (beam) is F/N and the maximum moment is

$$\text{Moment} = \frac{Fl^2}{N8}$$

and the maximum stress is the familiar MC/I

$$\text{Stress} = \frac{\text{moment} \times 1/2 \text{ conductor thickness}}{\text{moment of inertia of one conductor}}$$

$$\text{Stress} = F \times \frac{l^2}{8} \frac{\frac{\text{conductor thickness}}{2}}{\frac{1}{12} \text{ conductor width} \times \text{conductor thickness}^3}$$

The stress will usually come out to be about 50,000 pounds per square inch at the current at which the limiting axial force is reached. The inference is that the coil will fail by bowing out of the straight side. Actually, however, two important factors enter to reinforce the coil at this point:

1. As soon as a very slight bow has occurred, the beam is much stronger.

2. The core, backed up by a heavy steel base, takes much of the stress as soon as a slight deflection occurs.

The actual strength is very difficult to calculate. Tests to be described, however, show that this stress is not the limiting one.

4. *Resistance against axial movement.* A number of coils were made and the strength measured by actually pressing the inner coil out of the outer coil in a testing machine. The force required for the coil shown in Figure 4 was of the order of 600 pounds to start the coil moving. The cement used is a plastic, and its resistance is viscous in nature. The force required to move the coils very rapidly, as they do when actual failure occurs, is certainly higher, but it is not possible to say how much. From these tests the effective shearing strength of the cement was determined and strengths of other size coils calculated. A high-strength thermosetting cement was found to be practical for those designs made for high thermal capacity in oversize frames, to make the mechanical limit comparable with the high thermal limit. The strength of this cement is more than twice that of the plastic cement. The designs with which this cement is used have oversize copper, so that the tensile strength of the coil is increased about in proportion, and as they have, in general, more than four turns, the unwinding force was not a limit. The mechanical strengths were thus made higher in these designs in about the same ratio as the thermal limits.

Strength Tests

The critical strengths are evidently (1) the axial displacement resistance and (2) the bowing of the straight side of the coil.

The possible initial axial displacement in a coil the next size smaller than Figure 3 was deliberately made 0.16 inch by displacing the last coil layer. According to Clem's method, simplified, the axial stress was then 650 pounds at 100 times normal current, while the resisting force averaged 600 pounds. This transformer withstood 100 times normal current, as a fully offset wave could not be obtained, but at a slightly greater current the secondary coil was violently ejected, taking the core with it. Once the secondary coil moves, the force is increased, and although the current lasted only three cycles, there was ample energy delivered to do considerable damage.

Another unit was very carefully centered and cemented with high-strength cement so that an expanding force of about 8,200 pounds per inch, with a theoretical stress in the straight side, as a beam, of 98,000 pounds per square inch

at a current of 350 times normal could be developed. (Force per inch of coil length = 8,200 pounds.) This transformer actually withstood this current without damage. When the current was raised to 470 times normal fully offset, a bowing of the primary coil was produced. (Force per inch of length = 14,700 pounds.) The resulting bow did not destroy the transformer, but produced distortion of the core and frame.

The final justification of the methods consisted of calculating the mechanical limit of a number of designs, and testing

Table I

150/5 = 8 primary turns to	240 secondary turns
100/5 = 8 primary turns to	160 secondary turns
75/5 = 8 primary turns to	120 secondary turns
50/5 = 8 primary turns to	80 secondary turns
40/5 = 8 primary turns to	64 secondary turns
30/5 = 8 primary turns to	48 secondary turns

them all (over 15 units) at currents exceeding the calculated limits, without damage. Higher currents produced axial movement in some units not perfectly centered, and still higher currents destroyed a few units which were centered, by bowing of the coil.

The final strength limit is fixed by the axial force on account of the assumption that these coils cannot invariably be initially centered closer than 0.1 inch. This dissymmetry is caused by a number of variations.

1. Dissymmetry due to leads.
2. Misplacing of the last (partial) layer.
3. Dissymmetry of assembly of the coils.

It is possible to control these factors in manufacture, but as it is not practical to make any tests for symmetry of the final assembly, it is considered that it would not be conservative to assume that the initial displacement would be less than 0.1 inch.

Metering and Relay Performance

The method of calculation used has been described previously.[8] The quality of the problem to be solved can be illustrated by pointing out that the errors in transformation vary (other things being equal) approximately as follows:

Errors increase with increasing secondary burden in ohms.

Errors increase with decreasing secondary turns (about as the square).

The mechanical limit (limited by forces) increases directly with decreasing secondary turns.

The thermal limit (considered in times normal) also increases with decreasing secondary turns (again almost directly).

The internal burden, however, decreases with decreasing secondary turns (about as the square).

The relations are not exact but they illustrate the conflict between errors on the one hand and burden and current limit on the other.

Application Study

The design study is of fundamental importance, but the method by which it has been applied is of greater general interest, because it is based on ideas which are new and by which a unique result has been obtained.

Obviously a practically infinite number of current transformer designs would be required to suit all the applications exactly. Therefore, some study must be made of the field of application to allow the fundamental design principles to be applied. A limited number of standard designs is obviously desirable, but it is not clear, at first glance, what to choose as standard. The following paragraphs describe the evolution of standard lines of transformers.

Standard Overcurrent Limits: Correlation With Circuit Breakers

Standard switchgear practice[9] is to specify, for the current transformer, overcurrent capacity equal to that of the circuit breaker with which it is used. The logical conclusion, therefore, is to design a standard line of current transformers to go with each of the commonly used circuit breakers. Four standard lines of current transformers in each insulation class are indicated, designed for fault currents of 14,500, 26,000, 36,000, and 46,000 amperes.

Standard Cores

Four core sizes have been found to be sufficient to cover the range of requirements from 5- to 15-kv insulation classes. More details on the cores are given later.

Standard Primary Coils

Given the insulation class, overcurrent limit, and core size, the primary-coil design is fixed, since the primary turns and copper section are determined by the overcurrent limit. The maximum possible number of primary-coil designs is then the product of 3 (insulation classes 5, 8.7, 15 kv) times 4 (overcurrent limits listed above) times 4 (core sizes) or 48. As shown in Table III, the useful number of designs is only 28. Furthermore, as shown in the same table, 12 of these designs are taken from the standard commercial line of transformers. Finally, then, only 14 special primary coils need to be designed, built, or carried in stock.

Secondary-Coil Design

In making the core designs and the designs for the primary coils, space will have been left for a secondary coil to have substantially the same total copper cross section as that of the primary coil, so that the overcurrent density in the secondary copper will be substantially the same as in the primary, regardless of the number of secondary turns.

Table II. ASA Metering Accuracy Classes Related to Secondary Turns (for Any Core)

Secondary Turns*	Burden 0.1	Burden 0.5	Burden 2.0
240	0.3	0.3	0.3
200	0.3	0.3	0.6
160	0.3	0.3	1.2
120	0.3	0.3	2.4
100	0.3	0.6	2.4
80	0.3	0.6	4.8
60	0.6	1.2	
40	0.6	4.8	
30	1.2	4.8	

* Nominal: The actual value is slightly smaller so that the actual current ratio will come out right.

The various nominal current ratios are obtained with a given primary coil by varying the number of secondary turns. For example, the eight-turn primary coil in core number 1 is used to make the ratios shown in Table I.

As the number of turns is reduced, the copper section in each turn must be increased. Obviously all ratings will carry 150 amperes continuously. The last one, for instance, the 30/5, may accurately be described as 150/25. The design of the secondary coil is obviously very simple.

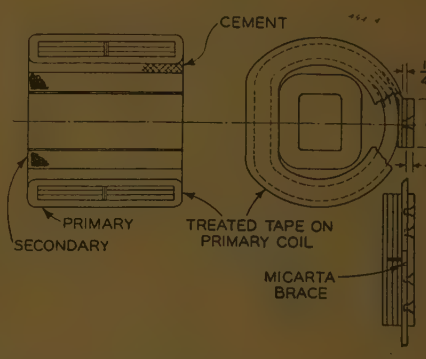


Figure 4. The primary coil shown is that of the 200-ampere rating, wound of heavy copper strip in two sections

The end (outside) turns are held down by the brace which holds the terminals together, and by the treated tape. The secondary and primary are held together by cement

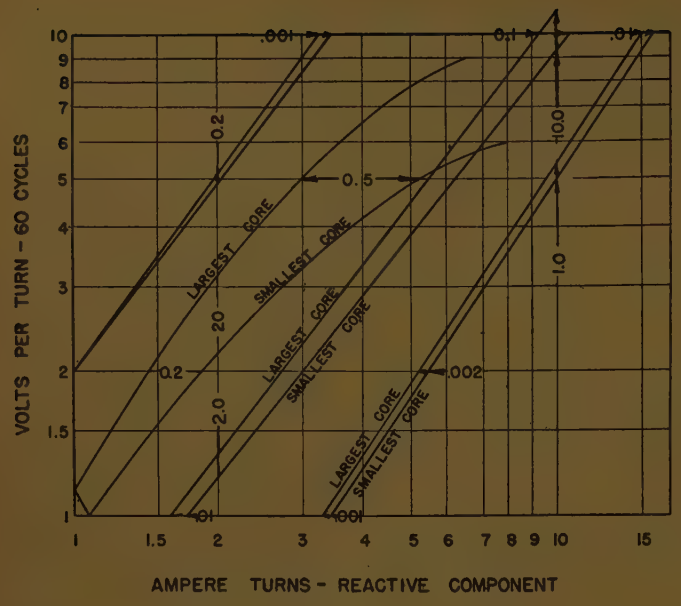


Figure 5. The saturation curves for the cores are practically identical over the lower (metering) range, so that the metering performance of all cores is the same

The curves vary widely over the upper (relaying) range, but as the relay performance rating is a function principally of the saturation voltage, it is not too difficult to work out relay performance ratings for each one

Performance

As the number of secondary turns is reduced, the performance becomes poorer. In a given core, the performance depends only on the number of secondary turns. The performance can be calculated for each number of secondary turns and core size, but the great amount of numerical work creates an impossibly tedious problem. The problem was made practical only by designing the cores as follows:

1. The saturation curves, in the lower (metering) range were made substantially identical (Figure 5).

2. The proportions of the window opening were so adjusted that the internal reactance of the secondary coil comes out the same in any core (for a given number of secondary turns).

This means that the calculation carried out for one core holds for any core, and that the performance for any design can be read from a single simple table, such as Table II.

Final Standard Lines of Transformers (Table III)

Each primary coil in Table III represents a line-of transformers covering a rather small range of current ratios, as in Table I, and the performance of each of these ratios can be read from Table II. The total possible number of designs is the product of the number of primary coils times the number of secondary coils used with each primary. The number of ratios in each line is small because the highest ratio is that which has the normal maximum number of turns in the secondary, while the lowest ratio is the one which has the minimum number of secondary turns for usable performance. However, as the table shows 28 lines of transformers, which, with at least six ratios in each line, is 168 new designs, it might seem that a great deal of design work is yet to be done. However, after the 14 special primary coils have been designed, the design of the secondary coils is almost simple clerical work.

The final complete table of transformers would require considerable space and is not included. Each space in Table III is expanded, in the manner of Table I, into a line of transformers. Each line has the one *continuous* primary-current rating in all ratios. The performance characteristics fall into a consistent pattern.

The transformers are constructed in a similar way, and the fact that only four sets of mechanical parts and only 28 primary-coil designs are required reduces manufacturing expense.

The problem of selection is almost completely solved for the application engineer, since he can very quickly find the design he wants in the complete tables furnished by the manufacturer.

Control of quality is much easier. The user of the transformers can be more certain that transformers selected from a standard line of proved performance, will be more satisfactory.

Table III

Mechanical Limit (Amperes)	Insulation Class (Kilovolts)	Core No. 1 Primary Turns	Core No. 1 Thermal Limit	Core No. 2 Primary Turns	Core No. 2 Thermal Limit	Core No. 3 Primary Turns	Core No. 3 Thermal Limit	Core No. 4 Primary Turns	Core No. 4 Thermal Limit
14,500	5	8*	12,500	12	14,500	14	17,000		
26,000	5	4*	26,000	7	26,000	8	30,000		
36,000	5	3*	36,000	5	36,000	6	50,000		
46,000	5	2*	50,000	3	50,000	4	60,000		
14,500	8.7			8*	12,500	14	14,500		
26,000	8.7			4*	25,000	8	26,000		
36,000	8.7			3*	36,000	6	36,000		
46,000	8.7			2*	46,000	4	50,000		
14,500	15					8*	12,500	12	14,000
26,000	15					4*	25,000	7	27,000
36,000	15					3*	36,000	5	36,000
46,000	15					2*	46,000	4	50,000

* These primary coils are taken without change from the standard commercial lines of transformers; the transformer construction and dimensions are standard. The coils with 8, 4, 3, 2 turns, respectively are the coils for standard 150, 300, 400, and 600 amperes, respectively. The other designs are made with special primary coils. The higher-strength coil cement is used to resist the higher mechanical forces generated by the higher values of ampere turns. These designs are accordingly more expensive, but of course the performance of these designs is much better.

References

1. American Standard C57, Transformers, Regulators, and Reactors, 1943. Paragraphs 4.045–4.046.

2. American Standard C57, Transformers, Regulators, and Reactors, 1943. Paragraph 4.031.

3. American Standard C57, Transformers, Regulators, and Reactors, 1943. Paragraph 4.030.

4. American Standard C57, Transformers, Regulators, and Reactors, 1943. Paragraph 4.032.

5. Mechanical Forces in Transformers, J. F. Clem. AIEE Transactions, volume 46, 1927, pages 577–80.

6. Electrische Maschinen (book), H. Richter, Volume III, pages 88–9.

7. The Calibration of Current Transformers by Means of Mutual Inductances, C. L. Fortescue. AIEE Transactions, volume 34, 1915, pages 1599–1615.

8. A Simple Method for Determination of Ratio Error and Phase Angle in Current Transformers, E. C. Wentz. AIEE Transactions, volume 60, 1941, October section, pages 949–54.

9. NEMA Switchgear Standards, 37–39, SG 4-21

10. Two Cases of Calculation of Mechanical Forces in Electric Circuits, H. B. Dwight, AIEE Transactions, volume 46, 1927, pages 570–

Totalizing Contents of Aircraft Fuel Tanks

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THE requirement of totalizing two or more liquid levels in aircraft fuel tanks falls into two broad classifications. The first involves the addition of levels where the sequence of use is definitely established. This type is best exemplified by considering a long wing tank where the dihedral angle makes it necessary to have more than one transmitter in the tank to obtain complete coverage of the fuel contents. Figure 1 shows such an arrangement. With the fuel outlet as shown, transmitter 2 must function first. In other words, the sequence of operation is fixed so that the operation of transmitter 2 is always followed by that of transmitter 1 as fuel is withdrawn from the tank.

The second category covers the totalization of several liquid levels in different tanks where the sequence of use is not established, but where single indication of the total fuel is desired. For the three separate tanks each with its transmitter, shown in Figure 2, the sequence of use of the fuel in the three tanks may vary widely. Such variations may be caused by the puncture of one or more tanks by gunfire, by the necessity of trimming the plane, or by other abnormal changes.

Principle of Operation

In order to explain the operation of the totalizing circuit it is desirable to show first the basic principles of a similar circuit when used in the conventional manner. Figure 3 shows a typical liquid-level transmitter now used in aircraft fuel tanks. The cork float at the end of the lever floats on the surface of the fuel. As this surface level changes the motion of the float is transformed through suitable gearing and sealing mechanisms to a change in resistance of a potentiometer unit located in the head of the transmitter.

Figure 4 shows schematically this potentiometer connected to the element of the indicator on the instrument panel. This element has two coils, located 140 degrees apart, one of which is connected directly across the battery through a series resistor. The current through this coil is constant except for changes in the line voltage. The other coil is shunted by the potentiometer in the transmitter. The amount of shunting resistance, and hence the current in the indicator coil, is therefore dependent upon the position of the transmitter float and varies with the liquid level.

The vector diagram of Figure 5 further illustrates the principle of operation. The permanent-magnet rotor to which the indicator pointer is attached aligns itself with the resultant vector, a vector obtained by the addition of the constant-current-coil flux and the variable-current-coil flux. The general design of this indicator is similar to one described in an earlier paper,[1] except it has been made up in a ratio-instrument[2] form for this application.

Sequence Totalizing

In a simple two-transmitter-sequence totalizing circuit the variable-current coil of Figure 4 is double wound. Each section of this double winding is connected to a transmitter as shown schematically in Figure 6. The flux from the variable-current coil is the sum of the fluxes produced by the currents flowing in the two parts of the double-wound coil. The conditions of the system described in Figures 4 and 5 are duplicated.

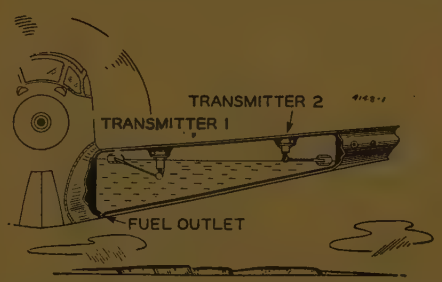


Figure 1. Installation showing necessity of two transmitters to obtain complete fuel coverage

Only one value of resultant flux is possible for any tank content; therefore, the indicator reads the correct fuel quantity. The indicator-scale distribution is established by the shape of the tank and the values of the circuit resistances.

Nonsequence Totalizing

Since the indicator must show correct readings regardless of how the fuel is distributed in the tanks being totalized, the nonsequence-totalizing system presents a different problem from the sequence-totalizing system just described.

As in the case of sequence totalizing, there is a separate winding on the variable coil in the indicator for each transmitter used. In order to make the indication of the total fuel quantity independent of the sequence of consumption, the increment of flux and ampere turns per gallon of fuel associated with each winding of the variable coil must be constant and of the same value for all tanks. The total ampere turns produced by the variable coil can then be represented by the equation:

$$NI = \Delta ni(g_1 + g_2 + g_3 + \ldots) \qquad (1)$$

where NI is the total ampere turns produced by the variable coil, Δni is the increment of ampere turns per gallon, and g_1, g_2, and g_3, are the gallons present in tanks 1, 2, and 3, respectively. In order to obtain the condition shown by this equation consideration must be given to:

1. The shape of the tanks.
2. The dimensions of the transmitters.
3. The electrical circuit.

The simplest tanks to totalize would be those having a straight line as the plot of gallons versus float angle. However, it is impractical to meet this condition

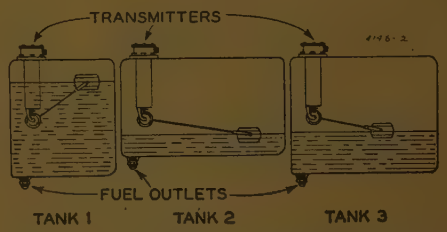


Figure 2. Installations in three different tanks with total fuel in all tanks to be indicated on a single dial

and the nearest practical approach is a tank with uniform cross section at all liquid levels. With a tank of this construction it can readily be shown that:

$$x = L(1 - \cos\theta) \qquad (2)$$

where x is the depth of the liquid, L is the length of the float lever, and θ is the angle formed by the float lever and a perpendicular to the liquid level. This equation is shown graphically in Figure 7. For uniform-cross-section tanks, the dimensions of the transmitters can be designed so that θ is kept between 60 degrees and 120 degrees to utilize the portion of the curve which is almost a straight line. Although a constant cross section is not always obtainable, it is often possible to vary the transmitter dimensions in order to utilize the curve of Figure 7 in such a manner as to compensate for the shape of the tank. Control over the characteristics of the electrical circuit can be obtained by introducing corrective resistance values R_1, R_2, R_3, and R_4 into the

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Figure 3. A typical liquid-level transmitter

proper parts of the circuit as shown in Figure 6. These resistances also provide an additional means of compensating for the nonlinear relation between gallons and float angle which may exist even when the optimum dimensions of the transmitter have been selected.

For example, consider a single section of the dual-wound coil and its accompanying transmitter potentiometer in Figure 6, where R_c represents the variable-coil-circuit resistance; R_{T1} and R_{T2} the transmitter potentiometer resistances. R_{T1} and R_{T2} always add to give R_T the total potentiometer resistance. The total ampere turns necessary to operate the indicator element are known from its design when used in the conventional manner. The maximum ampere turns are contributed by any one tank when that tank is full. The ratio of these maximum ampere turns to the total maximum ampere turns is equal to the ratio of the tank capacity to the capacity of all the tanks being totalized. The maximum ampere turns occur when g_1, g_2, and g_3 of equation 1 are equal to the capacities of tanks 1, 2, and 3, respectively.

To insure uniformity of positioning of the sections of the multiwound coil, the individual windings are all wound in a

Table I. Variations In Indication With Changes in Use Sequence

Total Fuel, Gallons	Average Indicated Gallons With Seven Various Sequences of Use						
	A	B	C	D	E	F	G
0	0	0	0	0	0	0	0
100	100	110	98	92	94	98	90
200	200	208	206	198	198	192	198
300	300	294	308	298	298	288	288
400	400	386	410	400	400	400	386
500	500	500	500	500	500	500	500

Sequence of use *A*—As calibrated.
Sequence of use *B*—Tanks 1, 2, and 3 simultaneously.
Sequence of use *C*—Tank 2, tank 1, tank 3.
Sequence of use *D*—Tank 1, tank 2, tank 3.
Sequence of use *E*—Tank 1, tank 3, tank 2.
Sequence of use *F*—60 per cent tank 2, 60 per cent tank 1, 100 per cent tank 3, 40 per cent tank 1, 40 per cent tank 2.
Sequence of use *G*—60 per cent tank 1, 60 per cent tank 3, 60 per cent tank 2.

Tank 1 contents—200 gallons.
Tank 2 contents—100 gallons.
Tank 3 contents—200 gallons.

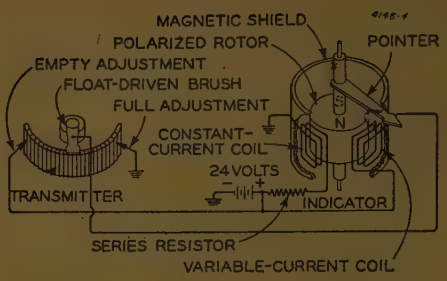


Figure 4. Schematic cut-away view of indicator and transmitter

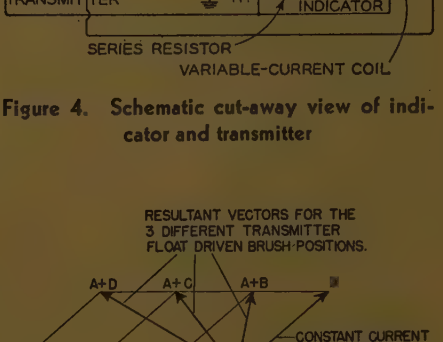


Figure 5. Vector diagram of fluxes for three currents in the variable coil

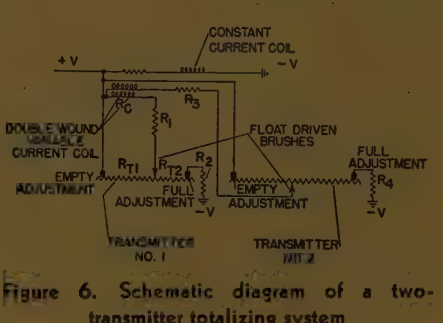


Figure 6. Schematic diagram of a two-transmitter totalizing system

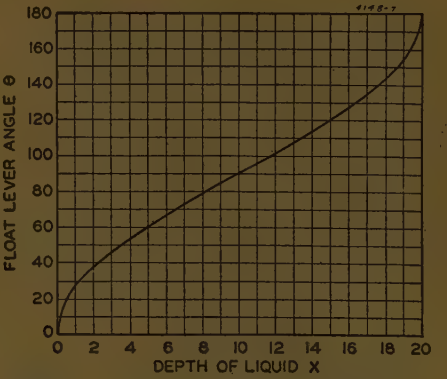


Figure 7. Curve of depth of liquid versus float-lever angle for a tank of constant cross-sectional area

$$x = L(1 - \cos\theta);\quad L = 10$$

single operation keeping the several wires as closely together as possible. Each winding of the coil then has the same number of turns.

To meet the conditions of equation 1 it is necessary to vary R_1, R_T, and R_2 so that the curve of the current in the coil versus gallons in the tank is a straight line. Since in practice this straight line cannot often be attained, the closest possible compromise must be used.

To plot this current-versus-gallon curve, an intermediate curve of the tank calibration must be used. The calibration curve shows the liquid level in the tanks versus gallons. This liquid level in turn is translated into angular movement of the float lever and finally into values of per cent R_T where

$$\text{Per cent } R_T = (R_{T1}/R_T)100 \qquad (2)$$

The amount of current in the variable indicator coil for various liquid levels can then be readily calculated by solving the simple circuits of Figure 6, and the resistance values determined to give the closest approximation to a straight-line relationship between current in the variable coil and gallons in the tank. Similar curves must then be obtained for the other tanks.

When the values of R_1, R_T, and R_2 are chosen for one of the tanks the calculations for the remaining tanks become simple, providing the tanks are the same shape. When this is true the values of R_1, R_T, and R_2 are multiplied by a suitable constant proportional to the contents of the tank being considered in relation to the total contents of the system. If the other tanks are of different shapes, the values of R_1, R_T, and R_2 must be determined for each by the same process used on the first tank, so that for each tank the curve of current versus gallons is a straight line of the same slope.

Application of Nonsequence Totalizing

Figure 8 is a photograph of a totalizing-indicator element used to read the total fuel in three tanks. The variable current coil located beneath the magnetic shield is triple wound. Manufacturing difficulties in producing this multiwound coil limit the number of windings and hence the number of tanks which can be totalized with a given-size element. Figure 9 shows the complete instrument using the element in Figure 8. The scale



Figure 8. A three-transmitter totalizing indicator element

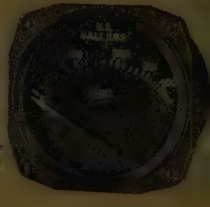


Figure 9. A three-transmitter totalizing indicator using element of Figure 8

Ring Busses for Generating and Transmission Stations

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IN recent years, the Commonwealth Edison Company has adopted bus layouts for generating stations and transmission terminals which differ greatly from those installed in its older stations. The purpose of this paper is to describe these developments and to discuss the application of the ring-bus principle to high-voltage installations. It appears to be an appropriate time to review the characteristics of various bus layouts in view of possible postwar expansion and rehabilitation of existing stations.

The basic problem in determining the most desirable bus layout is to obtain reliability, flexibility, and adequate relay protection, at a reasonable cost. In the earlier period of development of bus layouts in large metropolitan stations, the most important of the above factors was reliability of supply. It was found that a duplicate power supply for all transmission lines emanating from the generating station was essential for reliable service. However, as methods of station construction improved and advances were made in the development and design of station equipment and bus-protection schemes, the requirements for bus layouts in generating, distributing, and transmission stations have also changed. In addition, operating experience has indicated certain desirable features which should be incorporated in bus layouts. This paper will show how the bus arrangements used in the Commonwealth Edison system have been changing through a period of years to take advantage of improved equipment and experience in the operation of the system.

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12-Kv Busses

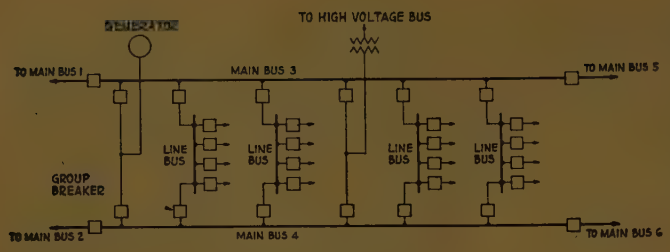
Description of Bus Layouts

The conventional type of double-bus arrangement, which was used in the Commonwealth Edison Company up to 1936, is illustrated in Figure 1A. In this arrangement, there are two sets of main busses, each set consisting of several sections separated by section tie breakers. The generators, high-voltage transformers, and line busses which feed the outgoing lines are so arranged that they may be connected to either or both sets of main busses. The substations supplied by the outgoing lines have a multiplicity of supply lines so selected that the outage of one line bus does not result in loss of load. The number of lines connected to each line bus has varied; earlier stations have three lines per line bus, while later stations have four lines per line bus.

Several variations in this conventional bus have been used. At one station the two sets of main busses are connected through two reactor ties as shown in Figure 1B. The purpose of these reactors is to reduce short-circuit currents with the two main busses operating in parallel. In this station, some of the generators, transformers, and line busses are operated closed to one of the two main busses, and the remainder to the other main bus.

In another case, reactors were used between the main bus sections as shown on Figure 1C. The advantage of this arrangement over Figure 1B is that it permits parallel operation of the two main busses with opposite bus sections solidly tied, and yet provides a reduction in short-circuit current. However, these reactors create a disadvantageous situation with respect to power flow on the outgoing lines. These lines operate in parallel at the substation end, with the result that any phase-angle displacement at the generating busses results in undesirable circulating currents.

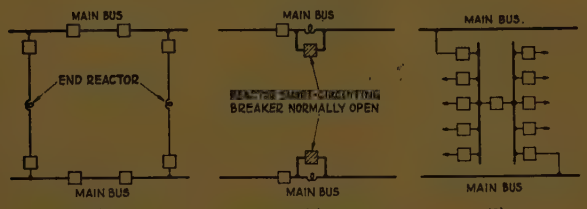
At one station the simplification shown on Figure 1D was used. In this



(A) FUNDAMENTAL DOUBLE MAIN BUS LAYOUT

Figure 1. Double-main-bus layouts for generating and distributing stations

Only operating disconnecting switches shown in diagrams



(B) DOUBLE MAIN BUS WITH END REACTORS

(C) DOUBLE MAIN BUS WITH REACTOR TIES BETWEEN BUS SECTIONS

(D) METHOD OF REDUCING NUMBER OF GROUP BREAKERS BY 25%

layout, the line busses are directly connected to only one main bus and each pair of adjacent line busses are tied through a group breaker. This scheme reduces the number of group breakers required for a pair of line busses by 25 per cent. Because of the method of operation used, the loss of flexibility entailed by the use of this scheme is of negligible importance. However, this arrangement required a larger group breaker, inasmuch as a group breaker may be required to supply two line busses.

In the early 1930's, a new distributing station was planned to receive power from the 66,000-volt transmission system for distribution at 12,000 volts. This station was to be built for an ultimate of 80 12,000-volt lines. At the time, operating experience with the conventional double-bus arrangement of Figure 1 was reviewed, and it was found that the number of bus outages was so low that the duplication of busses was not justified. This study resulted in the adoption of the bus layout shown in Figure 2. Two rings are used in this layout because of the sectionalized operating of the 12-kv system. This method of operation is described in another part of the paper.

This new type of bus layout is fundamentally a ring bus in which the distribution lines are connected directly to the main ring. In this way, the main busses used in the conventional double bus are eliminated. The use of this plan results in the elimination of a great deal of bus equipment and approximately 40 per cent of the number of breakers required other than line breakers. In this station five outgoing lines are connected to each bus section, compared to a maximum of four lines per line bus in the older stations. One disadvantage of this bus arrangement is that the supply transformers are connected to the same bus

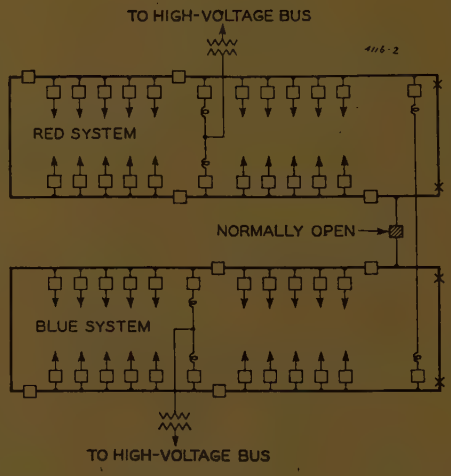


Figure 2. 12-kv ring-bus layout with one breaker between bus sections

Bus sections may be added by cutting ring at points indicated and extending the main rings

as the lines, whereas in the double bus the transformers are connected to the main busses while the lines are connected to the line busses. The ring bus has an advantage over the straight bus with respect to the concentration of current in the bus. With the ring bus, there is a more equal distribution of the current due to the fact that the supply current can flow in two directions to the load.

The operating experience with the layout of Figure 2 proved to be satisfactory except for one situation which is inherent in all bus schemes which have only one tie breaker between bus sections. With this type of bus separation, trouble in a bus section breaker can cause an outage of two bus sections, thus interrupting a sizable proportion of the entire station. Although such an occurrence is extremely rare, it would be advantageous from a system point of view, if any one case of trouble would cause the outage of only one bus section. This improvement was obtained in a later development shown in Figure 3 by using two breakers in series between bus sections. The only essential difference between Figures 2 and 3 is in the connection between bus sections.

Operation of 12-Kv Busses

The design of a fundamental bus layout is closely related to the method of operating the system. In the Chicago system, the principle of sectionalization has been one of the corner stones of reliable power supply. With this type of operation any one case of trouble is confined to approximately one-half of the system. All generating stations and transmission terminals are divided into two parts, known as the red and blue systems. At some stations, the two systems are normally operated completely separated from each other. At other stations, the red and blue systems are normally operated in parallel, but automatic sectionalization takes place whenever a heavy fault occurs on either system. This automatic sectionalization which is accomplished by relay action separates the station into two parts without causing the outage of any lines or equipment. The method of operating a particular station depends on the magnitude of short-circuit currents and the balance between the load and capacity. It would be desirable to operate sectionalized at all stations, but in some locations it is not economical to provide sufficient equipment to make possible this method of operation.

The ring bus is well adapted to the type of sectionalized operation used in Chicago. In the bus layouts shown in Figures 1 and 3, two rings are used, one ring constituting the red system and the other the blue system. An important advantage of the ring bus is that it can operate sectionalized even though a bus outage occurs. In the double-bus layout, Figure 1A, a main bus outage causes the loss of a considerable amount of equipment, with the result that when such an outage occurs the red and blue systems must be operated solidly tied, and without automatic sectionalization.

Various methods are provided for the normal operation of the two rings. In the diagram of Figure 2, the two rings can be paralleled directly, through reactors or operated completely separated. In the diagram of Figure 3, the rings may be tied through reactors, or tied solidly through connections to an adjacent station. There is also a tie between the rings through the two windings of the supply transformer; however, this tie is of relatively high reactance and represents a weak link between the systems. The two-winding generator shown constitutes a relatively heavy tie between the systems. Automatic sectionalization is provided to disconnect this generator from the system in trouble. The

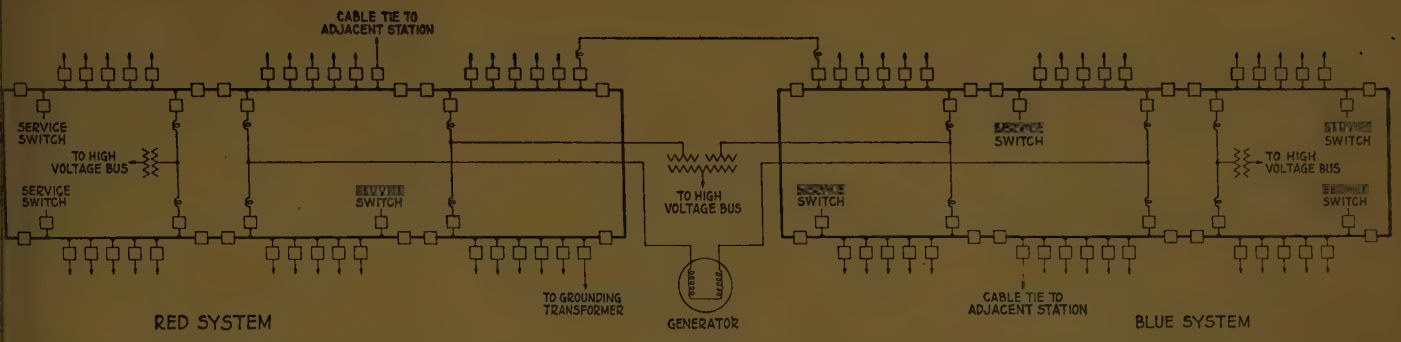


Figure 3. 12-kv ring-bus layout with two breakers in series between bus sections

method of operation actually used at these stations will depend on the magnitude of fault currents and on the balance between load and capacity as previously mentioned.

Bus Protection

Although relay protection of station busses greatly improves reliability, in the earlier Chicago stations no bus protection was used. The current transformers then available were considered to be too hazardous from an insulation viewpoint to be installed in station busses. Also, the electrical performance of these transformers and the types of relays available were not sufficiently good to make possible a reliable relaying system. On the other hand, because of the methods of construction used bus faults were considered to be too infrequent to justify the added risk incurred by the use of the protective equipment then available. This combination of circumstances resulted in all the earlier stations in Chicago being installed without bus protection.

In the early 1920's, the isolated-phase method of construction was developed and used for all station projects. This method of station construction makes possible a very simple and reliable type of bus protection known as the fault bus.[1] The operating record of this type of bus protection has been very satisfactory, inasmuch as all primary faults have been cleared correctly and quickly. In the past ten years, there have been 12 primary failures on the 66-kv and 12-kv busses at generating stations due to insulator and bushing failures and system operating errors, and these have all been cleared by the fault bus with a minimum of damage and disturbance to the system. In addition to these correct operations, there have been five unnecessary operations caused by low-voltage d-c and a-c circuits coming in contact with the fault bus. Some of the operations have been due to the failure of low-voltage circuits, and others to the improper use of the fault bus by personnel. An example of the latter case is the use of fault bus for grounding welders. These incorrect operations can be reduced to a minimum by using induction relays to prevent operation due to direct current, and by taking precaution to eliminate incorrect use of the fault bus by personnel.

An interesting point in connection with fault-bus protection is its application to the ring bus shown on Figure 2. If a fault occurs on a bus section breaker, the fault bus will instantly disconnect one of the busses to which the section breaker is connected. If this operation does not clear the fault, the other bus section connected to the breaker is cleared. In this manner, a fault on a section breaker may or may not interrupt two bus sections. The possibility of tripping two bus sections for one case of trouble can be eliminated by using two bus section breakers in series as shown in Figure 3. As discussed previously, this undesirable situation also exists in the double-bus layout.

While earlier stations were installed without bus protection, serious bus failures experienced by other companies, together with the development of improved current transformers and relays, have led to the addition of bus protection to these earlier busses. Conventional methods of bus differential relaying using induction overcurrent relays have been used. In one special case, impedance relays have been installed for bus protection. Up to the present time there have been no automatic operations of the bus differential or impedance relay schemes.

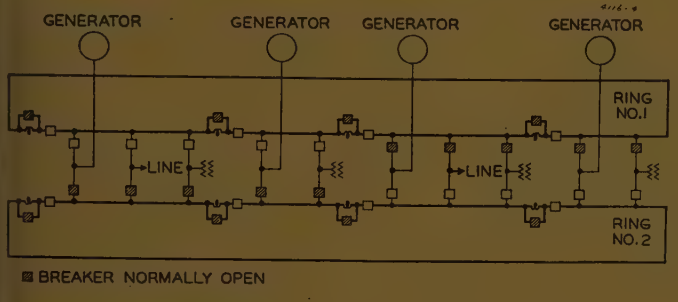


Figure 4 (left). 22-kv ring-bus layout with provisions for connecting any unit to either ring

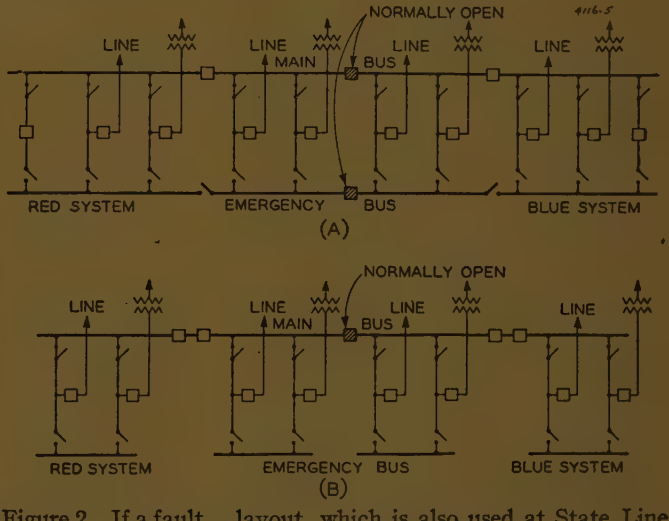


Figure 5 (right). Bus layouts used in the early development of the 66-kv system

A—Main-bus sections tied through one breaker

B—Main-bus sections tied through two breakers in series

22-Kv Busses

The first type of ring bus used in the Chicago area is shown in Figure 4. This bus was installed in a bulk power generating station operating at 22 kv. A detailed discussion of this type of ring-bus layout, which is also used at State Line station, has been published in a previous paper.[2]

This bus is a double ring in which each generator winding and transformer has connections to both rings. The bus sections of each ring are separated by a breaker and reactor in series with provisions to short-circuit the reactor by a second breaker. Experience has indicated that sufficient operating flexibility and reliability can be obtained with fewer breakers than the number shown in Figure 4.

The 22-kv ring busses are oil-filled metal-clad structures which are protected by a fault-bus system. An impor-

tant limitation of the arrangement used is that a bus section on one ring is part of the same steel structure as the similarly located bus section on the other ring, with the result that the fault-bus system cannot determine which of the two busses is faulted. This condition limits the reliability and, therefore, the flexibility of this type of double-ring bus.

66-Kv Busses

The transmission system within Chicago is a 66-kv underground system which in effect is a cable bus running the full length of the city, interconnecting

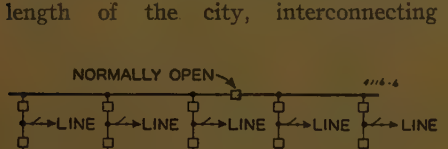


Figure 6. 66-kv bus arrangement which combines the principles of the double-main-bus and ring-bus layouts

sources of supply with load distribution centers.[1] This cable bus is operated in two sections known as the red and blue systems which correspond to the 12-kv red and blue systems. The principle of isolated phase construction is used at all the 66-kv terminals and applied to the transmission lines by the use of single-conductor cable.

Figure 5A shows the fundamental station bus layout which was used in the first busses constructed for this system. This layout is a double-bus arrangement in which there is only one circuit breaker per line or transformer with disconnect switches provided to connect the unit to either or both busses. During normal operating conditions the red and blue systems are sectionalized, with all units on their respective systems being operated in parallel. In order to limit the extent of line and transformer outages caused by a bus fault, the red and blue busses were each split into sections by means of circuit breakers. These breakers were installed only on the main bus since the emergency busses were to be used only when a main bus section was out of service.

An important limitation in the operation of this type of bus layout is that disconnect switches must be operated under load when it is desired to transfer units from one bus to another. Another disadvantage inherent in this type of layout is that a case of trouble on a bus section breaker interrupts a very large amount of capacity. As a result, the bus layout was rearranged to provide two breakers in series between bus sections, as shown in Figure 5B. This principle is the same as is used to limit the outage caused by a failure of a 12-kv bus section tie.

In the development of a new 66-kv terminal, which was part of the distributing station mentioned in the discussion of 12-kv busses, a radically different type of bus layout was adopted. This bus plan is shown in Figure 6 and is a combination of the double-bus arrangement and the ring-bus type of design. One advantage of this arrangement is that a station fault will result in the loss of a minimum number of units. For example, a bus fault necessitates the maximum loss of only one unit; whereas the layout of Figure 5 will cause a loss of two or more units. Another advantage is the increased flexibility of operation; there are many more possible arrangements of the system with a layout of Figure 6 as compared with Figure 5. Although

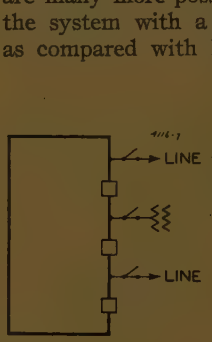


Figure 7. An initial installation of the bus arrangement shown in Figure 6

additional line and transformer breakers are required to obtain this increased flexibility, the layout of Figure 6 reduces the number of section tie breakers, so that the total remains approximately the same.

One disadvantage of the arrangement of Figure 6 is that it requires the opening of two breakers at each terminal to disconnect a faulted line or transformer, but this has not caused any trouble. As previously mentioned, one disadvantage of the older type of bus layout was the fact that to transfer units from one bus to another without de-energizing, it is necessary to operate disconnect switches under load. With the new bus layout, this type of operation is unnecessary.

In the bus layout of Figure 6, all breakers are operated closed except the bus section breakers separating the red and blue systems. The fault-bus system is used to protect the main busses, but the sections of bus to which the lines and transformers are connected are protected by the line and transformer relays. This illustrates one of the basic principles of the ring bus; from the standpoint of protection there is no bus because each section of the bus can be treated as a part of the unit to which it is connected.

In a later development, the bus plan of Figure 6 was adopted as the ultimate diagram. However, in the initial step there are only two lines and one transformer and the layout consists of the ring bus shown in Figure 7.

132-Kv Busses

The high-voltage transmission system outside Chicago consists primarily of overhead 132-kv lines. The principle of group-phase construction is used almost entirely in the transmission terminals of this system. A typical layout used for 132-kv busses on this system is shown in Figure 8. This arrangement is a double bus with one circuit breaker per unit and disconnect switches to connect the line to either of the two busses. A bus tie breaker is provided with necessary connections so that it can be used either as a bus tie or as a unit breaker. This type of bus layout was designed primarily for parallel operation of the 132-kv lines.

At the present time, the 132-kv system is operated sectionalized for the purpose of limiting the effect of faults on the stability of the system. With this method of operation, the type of bus shown in Figure 8 lacks operating flexibility. In order to take a breaker out of service either the bus tie breaker must be used for line protection, or the line must oper-

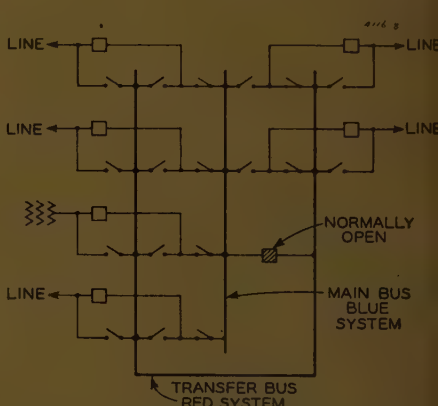


Figure 8. Double-main-bus layout used for 132-kv terminals

ate without a breaker. In the first case sectionalization cannot be maintained while in the second case no relay protection is provided for the line.

Practically all the busses on the 132-kv system were installed without bus protection. Operating experience has indicated the desirability of providing bus protection at these terminals, inasmuch as there has been an average of about one bus fault per year on nine 132-kv terminals of this system. These faults have been due to operating errors in grounding live busses, insulator and bushing flashover and conductors coming in contact with each other due to insulator breakage. Investigations have been made to determine the feasibility of applying bus protection to these terminals. It was found that fault-bus protection could not be used because of the design of the steel structures. This study also indicated that the application of differential protection to these busses would be very

complicated because the many combinations of system connections which are possible make it extremely difficult and costly to install a reliable and high-speed bus differential scheme that will satisfy all operating conditions. As a result of the difficulties involved in providing bus protection, most of these terminals are still operating without this type of protection.

Studies have been in progress recently which indicate that the principles of the ring bus could be advantageously applied to 132-kv terminals. One arrangement which has been studied is the single ring bus shown in Figure 9, which requires only one breaker for each line or transformer. The principal difference between this layout and that of Figure 6 is that the latter uses a larger number of breakers which results in a higher degree of flexibility. In both types of layout, the bus sections are relayed as part of the line or transformer connected to that bus section.

The most important advantage of the ring bus over the arrangement of Figure

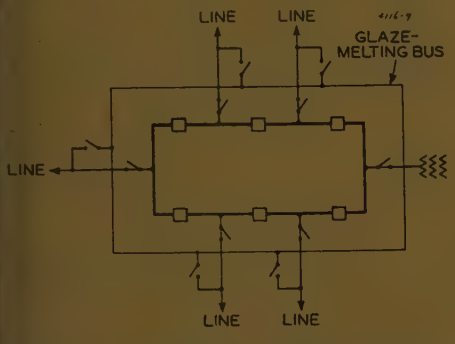


Figure 9. Single ring bus which has been studied for application to high-voltage terminals where all units are normally paralleled

8 is that bus protection is automatically provided. Also, a bus fault will affect system stability to a lesser degree if the ring bus is used, because as mentioned previously, the ring bus reduces to a minimum the number of units which must be disconnected for a bus fault. Another advantage of the ring bus is that any breaker in the ring can be taken out of service for maintenance work without disrupting the operation of the remainder of the system. However, if a fault should occur on a line or transformer while this maintenance work is being done, there is a possibility of losing other units in addition to the faulted unit. This characteristic of the ring bus is generally not of great importance during periods of light load when maintenance work is performed, but it does make the single ring unsuitable for use at terminals which are operated with the busses sectionalized. When a line or transformer is to be out of service for a long period, the disconnects on this particular unit can be opened and the ring re-established by closing the breakers of this unit.

An important problem arises in connection with operating arrangements for the melting of glaze on transmission lines. In the Chicago area, glaze melting is accomplished by isolating generators and transmission lines, and operating them with the transmission lines grounded and with reduced voltage on the generators. With conventional bus arrangements such as Figure 8, glaze-melting procedures may be effected by using one bus for load-carrying purposes and the other bus for glaze-melting purposes. Obviously this could not be done with the ring bus, but the problem can be solved by providing a separate glaze-melting bus with disconnect switches at

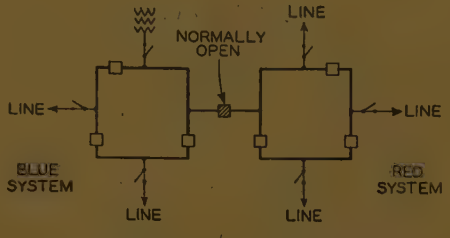


Figure 10. Double ring bus which has been studied for application to high-voltage terminals where sectionalized operation is used

each terminal for connecting lines to this bus as shown in Figure 9.

In the conventional type of bus, one set of potential transformers can be used to provide potential for metering and relaying. In order to provide for all possible operating conditions with the ring bus, it would be necessary to use more than one set of potential transformers.

Other arrangements of the ring bus have been studied. Figure 10 shows a double ring bus which requires one additional circuit breaker in order to accommodate the same number of lines and transformers as in a single ring. An advantage of the double ring bus is that a fault on one line does not break the ring. Also, this arrangement is desirable for those cases where the bus is to be operated in two systems, one ring being used for each system, without requiring any more breakers than are used on the double bus arrangement of Figure 8. However, the double ring does not have the same degree of flexibility as the bus layouts of Figures 6 and 8 for transferring units from one system to the other.

The ring bus shown in Figure 11 has desirable features for application at bulk-power generating stations. In addition to the advantages offered by the single ring bus previously mentioned, this layout reduces considerably the number of circuit breakers required without much sacrifice in the reliability of the lines, since transformer faults are very rare compared with line faults. For the case shown, only six breakers are required, while the layout of Figure 9 would require ten breakers.

The most desirable type of bus layout for any specific high-voltage installation depends primarily on the method of operating the system. At a terminal where all the units are to be normally operated in parallel the single ring (Figure 9) is the most suitable. If the terminal

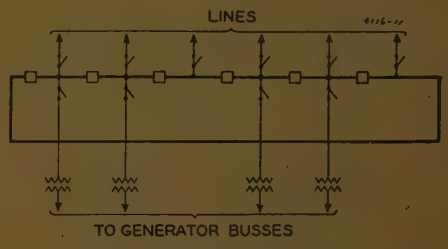


Figure 11. Single ring high-voltage bus which has been studied for application to bulk-power-supply generating stations

is to be operated sectionalized in two parts with only a limited need for transferring units from one system to the other, the double ring (Figure 10) is the logical choice. On the other hand, if maximum operating flexibility is required the principle shown in the bus layout of Figure 6 should be adopted.

This section of the paper has discussed the advantages of applying the ring bus construction to new 132 kv structures. No attempt has been made in this paper to show the cost of rebuilding existing installations to incorporate the ring-bus design, or whether the cost of such rebuilding would be justified.

Conclusion

This paper has described the development of high-voltage bus layouts in the Commonwealth Edison Company. It has attempted to show that the characteristics of the ring bus provide various advantages from the standpoint of flexibility of operation, ease of providing protection, simplicity of maintenance, and economy in investment.

References

1. System Connections and Interconnections—Chicago District, G. M. Armbrust, T. G. LeClair. AIEE Transactions, volume 49, 1930, pages 582–96.

2. Switching at State Line Station, T. C. White. AIEE Transactions, volume 53, 1934, January section, pages 148–56.

3. Substation Design Geared to the War Effort, G. D. Schrader. *Electric Light and Power*, July 1943, pages 42–4.

Interlamination Resistance

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IN laminated-core structures for transformers and rotating machines the need for adequate interlamination resistance becomes more necessary as the core structure increases in size. Where considerable pressures are exerted on the core laminations, a safe value of interlamination resistance must be maintained to prevent the stray losses in the core from becoming excessive. A method for determination of the minimum interlamination resistance required in terms of the core and its operating conditions, for any degree of permissible stray loss, is developed herein, resulting in a simple equation.

A short paper on this subject was prepared during the latter part of 1943, and encouraging comments on it led to the writing of this paper. It is recognized that this problem undoubtedly has been investigated or considered frequently, privately or otherwise, since the advent of the laminated core. In recent years, however, it has been the author's conviction that a more complete understanding should be had of the major factors influencing stray losses in the core. Although the consideration of the problem is not so rigorous as it could be, it is believed that, for designers of core structures, the resulting equations provide adequate protection in determining the minimum interlamination resistance required.

Figure 1 shows a section of a rectangular core of width W and height of lamination stack aW. The flux density and interlamination resistance are assumed to be uniform throughout the core structure, and the electrical resistance of the silicon steel of the laminations is negligible when compared with interlamination resistance. For analysis the core is divided by a neutral plane into two equal parts, each $W/2$ wide.

The voltage causing the stray-loss currents to flow is perpendicular to the laminations, and its value is a function of the lamination width. Consider an element dx in width, one centimeter in height, and aW long, having a resistance R, with the voltage causing the stray loss to be applied over the element length aW. The position of the element is x distant from the neutral plane of the core, and the core area enclosed becomes xaW. The resulting voltage due to the flux through this area will be that for one turn and will be concentrated across the element length aW. This can be stated as:

$$\frac{E}{N}=\frac{4.44fBaWKx}{10^8}\text{ volts per turn} \qquad (1)$$

where

f = frequency in cycles per second
B = flux density in kilogausses
a = ratio of height of stack to lamination width
W = lamination width in centimeters
K = space factor
x = distance of element from center of core in centimeters

The loss in the element due to the voltage of equation 1 is E^2/R watts, with

$$\frac{E^2}{R}=\frac{(4.44fBaWKx)^2}{R10^{10}}\text{ watts} \qquad (2)$$

The total stray loss for the laminated-core width $x=0$ to $x=W/2$ is:

$$\text{Total watts}=\frac{(4.44fBaWK)^2}{R10^{10}}\int_0^{W/2}x^2dx$$

$$=\frac{(4.44faKB)^2W^5}{24R10^{10}} \qquad (3)$$

Let the resistance

$$R=\frac{raW}{t}\text{ ohms} \qquad (4)$$

for the element having a cross section of one centimeter square and aW long, with r being the interlamination resistance in ohms per square centimeter per lamination and t the lamination thickness in centimeters. The weight of the core for the total stray loss of equation 3 is $0.5aW^2dK/453.6$ pounds, where d is the density of the core material in grams per cubic centimeter. Substitution for R in equation 3 and division by the weight of the core involved gives the average stray loss in watts per pound:

$$\frac{\text{Watts}}{\text{Pound}}=\frac{1.2211f^2B^2W_a{}^2Kt_a}{rd10^6} \qquad (5)$$

with W_a and t_a being expressed now in inches. With P in watts per pound for the core material at flux density B and frequency f, the ratio of the stray loss to core loss, stated as a percentage, %, becomes

$$\%=\frac{1.2211f^2B^2W_a{}^2Kt_a}{rdP10^4} \qquad (6)$$

and if we solve for the interlamination resistance r,

$$r=\frac{1.2211f^2B^2W_a{}^2Kt_a}{\%dP10^4}\text{ ohms per square centimeter per lamination} \qquad (6a)$$

It should be observed that the interlamination resistance r varies as the square of the frequency, the flux density, and the lamination width.

Further analysis shows, however, that the maximum stray-loss voltage occurs at the lamination edge of the core where $x=W/2$. From equations 2 and 4, with $x=W/2$, the stray loss in watts in the element, having one side as the side of the core, is:

$$\text{Maximum stray watts}=\frac{(4.44fBKW)^2taW}{4r10^{10}} \qquad (7)$$

The weight of the element one centimeter square is $aWKd/453.6$ pounds, and the maximum stray loss in watts per pound becomes:

$$\frac{\text{Watts}}{\text{Pound}}=\frac{3.6633f^2B^2W_a{}^2Kt_a}{rd10^6} \qquad (8)$$

with W_a and t_a expressed in inches.

With P in watts per pound for the core loss at B and f, the ratio of the maximum stray loss to core loss, stated as a percentage, %, is

$$\%=\frac{3.6633f^2B^2W_a{}^2Kt_a}{rdP10^4} \qquad (9)$$

and, if we solve for the interlamination resistance r,

$$r=\frac{3.6633f^2B^2W_a{}^2Kt_a}{\%dP10^4}\text{ ohms per square centimeter per lamination} \qquad (9a)$$

If equations 9a and 6a are compared, it will be observed that, with all factors the same, including the percentage, the interlamination resistance as calculated from equation 9a is three times the value obtained from equation 6a. But as the voltage causing the stray loss is maximum at the lamination edge of the core, the interlamination resistance should be calculated for this condition (equation 9a) rather than for the average stray loss (equation 6a). Furthermore, the max

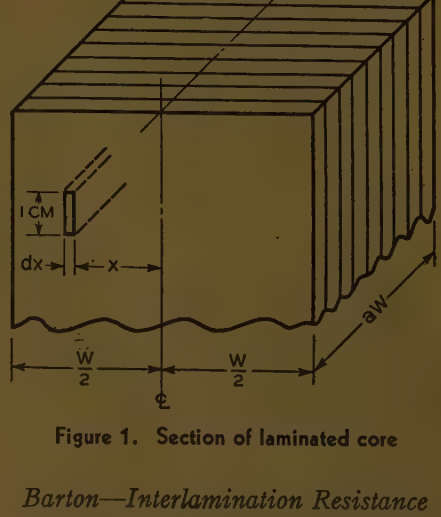


Figure 1. Section of laminated core

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mum voltage occurs where the lamination-edge condition is an important factor, that is, degree of burring and edge distortion which must be controlled to prevent low interlamination resistance.

If, for instance, in using equation 9a it is assumed that the per cent maximum stray loss to core loss at the lamination edge is nine per cent and the value of r is determined, then the per cent average stray loss to core loss becomes one third or three per cent. This is another way of showing the desirability of using equation 9a rather than equation 6a.

The interlamination resistance r is the average value in ohms per square centimeter of lamination and is the sum of all factors affecting its value. Not only does it include the resistance of the core-plating and surface oxides, but it also reflects the condition of the lamination surface and edges, stray metallic particles on the surface that may short-circuit adjacent laminations, and so forth. Furthermore and of major importance, it is the resistance that must be in effect at the pressure applied to the core and the temperature at which the core operates to assure the percentage of stray loss to be within the value chosen.

In applying equation 9a, the relation of B^2 to P should be examined. An examination of several commercial grades of silicon steel, as used for the laminations in rotating machines and transformers, in the flux density range of 8 to 15 kilogausses, shows that the ratio of B^2 to P can be considered a constant within an error of about plus or minus five per cent. This ratio states that the core loss varies as the square of the flux density, and for practical purposes the error involved is negligible in this application. This ratio also can be considered as a figure of merit for the steel used, as the greater its value the lower the core loss, for a given thickness.

If $B^2/P=C$ (a constant) is considered for the grade and thickness of the core steel used, the number of design curves that need be prepared for ready reference is simplified and reduced. Such curves can be drawn with the lamination width (W) as the independent variable, the interlamination resistance (r) as the dependent variable, with various values of the ratio of stray loss to core loss in per cent (%) as the parameter. The thickness (t), frequency (f), density (d), and space factor (K) are usually the same for many designs, with the first two, (t) and (f), usually not having more than two or three standard values.

Figure 2 illustrates such a design curve, using a 29 gauge (0.014 inch) commercial grade of transformer 52 silicon-steel sheet, with the core-loss-test specimen cut parallel to the grain of rolling and annealed. This test condition simulates the laminations in a transformer, and for this grade of steel $C=B^2/P=222$, being the average value in the flux-density range of 8 to 15 kilogausses. With $t=0.014$ inch, $f=60$ cycles, $d=7.5$, $K=0.92$ and % having values of 0.1, 1.0, 10, 100, and 1,000, the curves of Figure 2 were obtained. Such curves clearly show the values of interlamination resistance necessary to control the stray loss, as well as those values that will allow excessive stray loss with the danger of hot spots developing, further reducing the interlamination resistance progressively to the point where the core laminations may melt, causing eventual failure of the apparatus. Experience in applying these curves to such conditions just mentioned has substantiated the soundness of the analysis.

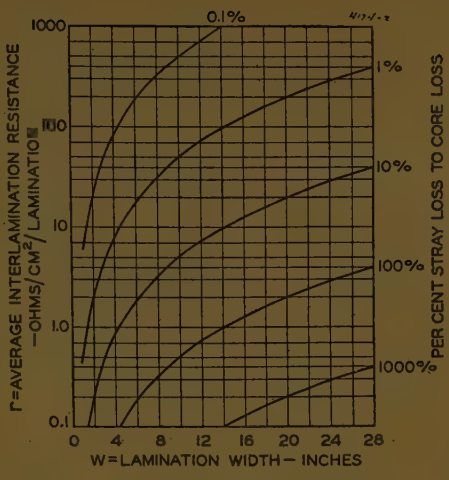


Figure 2. Variation of interlamination resistance with lamination width and per cent stray loss to core loss

For the conditions:

$$r=\frac{3.6633\,f^2B^2W_a^2Kt_a}{\%\,dP10^4}=\frac{0.50279\,W_a^2}{\%}$$

ohms per square centimeter per lamination

where

$f=60$ cycles $\qquad d=7.5$

$K=0.92 \qquad B^2/P=222$

$t_a=0.014$ inch

In the case of large transformers where the cores usually are bolted along the middle of the lamination, the pressure is maximum where the stray-loss voltage is minimum, with the pressure at the lamination edges being lower than at the center. By calculating from equation 9a the resistance for the maximum voltage, this value becomes the minimum interlamination resistance for the maximum pressure and operating temperature for the core, thus constituting the limiting and safe conditions. Whatever additional safety factor is desired can be multiplied by this calculated interlamination resistance. The same considerations also apply to the cores of rotating machines.

If the laminations on one side of the core are short-circuited together, the stray loss in the core increases four times as the voltage is doubled on the nonshort-circuited side. Furthermore, if the interlamination resistance is broken down, because of the increased voltage, the stray losses will increase more than four times.

In large symmetrical transformer cores bolted down the center of the laminations short-circuiting of the core by the bolts has no adverse effect, so long as the interlamination resistance is uniform throughout the core. For practical purposes, however, it is desirable to insulate all bolts in case of nonuniformity in the core structure, even though the ratio of total stray loss to core loss of the assembled core is within the allowed value.

The measurement of the interlamination resistance of the finished laminations becomes most desirable as a means of quality control in the manufacturer's plant toward assuring a resistance greater than the calculated minimum. The measurement of this resistance should be made on the specimens subjected to the maximum pressure, the maximum temperature and the maximum voltage to be encountered in the core. The rms voltage is given by equation 10,

$$E=\frac{1.4319fBaW_a^2K}{10^4}\text{ volts} \qquad (10)$$

where W_a is in inches.

A standard test method in general use is that for measuring interlamination resistance, as given in the American Society for Testing Materials *Standard Method A–34*. The test specimen is composed of 3x25-centimeter strips that can be processed in the same manner as are the laminations. It is essential that, whatever specimen is used to measure the resistance, it should be prepared in the same manner as are the laminations, so that not only the surface of the specimen but also the edge effects are included in the measurement.

The pressure can be applied readily, and the heating of the specimen can be accomplished by various methods. The testing at temperature is important, especially where core temperatures are higher than those heretofore considered normal, such as encountered in air-cooled inorganically insulated coils and cores. While the organic core-plate varnishes, when properly applied and baked on the lamination, will withstand pressure and voltage at higher core temperatures, the same core plate improperly processed or cured may flow and not give the desired insulation to withstand the voltage and pressure. Inorganic core plates are available, which will withstand lamination annealing temperatures and yield suitable values of interlamination resistance for large cores.

In conclusion it may be said that an equation has been developed wherein the minimum interlamination resistance can be calculated for the maximum stray-loss voltage to be encountered in a uniform core structure for a given percentage of maximum stray loss to core loss. This value of resistance is that to be in effect at the temperature and pressures at which the core structure is to operate. The importance of quality control of interlamination resistance, especially in large

Battery Booster Coils for Airplane-Engine Ignition Systems

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NONMEMBER AIEE

THE use of battery booster coils and problems in connection therewith requires an understanding of engine ignition-system design and operation. Figure 1 illustrates a complete ignition system consisting of ignition switch, two magnetos, radio-shielded harness, spark plugs and booster magneto. One magneto is illustrated completely assembled, and the other in skeleton form showing electric and magnetic circuits.

The magneto primary winding is grounded at one end and connected to the insulated breaker contact at the other end. A capacitor, as in all vibrator systems, is connected across the breaker contacts. The insulated breaker contact and ungrounded side of the primary winding are connected to a terminal on the magneto housing. This terminal is connected to ground through the ignition switch. When the switch is in the "off" position, a direct path to ground is provided the primary current. Therefore, when the breaker contacts open, the primary current is not interrupted, preventing induction of high voltage in the secondary winding. When the switch is in the "on" position, primary current is interrupted by opening of breaker contacts inducing a high voltage in the secondary winding. One end of the secondary winding also is grounded; the other terminates at the high-voltage insert on the coil. Current in the high-voltage circuit is conducted to the central insert of the distributor fingers by means of a carbon brush, from there to the high-voltage segment of the distributor finger, and across a small air gap to the contacts of the distributor block. High-voltage cables in the distributor block then carry it to the spark plugs where the discharges occur.

The distributor finger is secured to the large distributor gear which is driven by a smaller gear located on the drive shaft of the rotating magnet. The ratio between these two gears is always such that the distributor finger is driven at one-half engine crankshaft speed. This ratio insures proper distribution of the high-voltage current to the spark plugs in accordance with the firing order of the particular engine.

Practically all aircraft engines operate on the four-stroke-cycle principle. Consequently the number of sparks required for each complete revolution of the crankshaft is equal to one-half the number of cylinders. The number of sparks produced by each revolution of the rotating magnet is equal to the number of its poles. Therefore, the ratio of speed at which the rotating magnet is driven t that of the engine crankshaft is alwa half the number of cylinders on the engi divided by the number of poles on th rotating magnet.

Numbers on the distributor block d note magneto sparking order and do n represent the firing order of the engin Distributor-block position marked 1 mu be connected to cylinder 1, distributo block position 2 to the second cylinder t fire, and so on.

Sparks are not produced until the rota ing magnet is turned at or above a spec fied number of revolutions per minute, a which speed the rate of change of flu linkages is sufficiently high to produce th required primary current and resulta high-voltage output. The speed vari for different types of magnetos, the ave age is 100 rpm, and the minimum approximately 60 rpm. This is known a the "coming-in speed" of the magneto.

Starting of airplane engines has alway provided a problem from the ignitio engineer's point of view, and many ide have been introduced in an attempt t provide ideal ignition conditions witho increasing the weight of the system. source of external high-voltage current provided for starting purposes when co ditions make it impossible to rotate th engine crankshaft fast enough to produc coming-in speed of the magneto. Th may be in the form of a booster magnet high-voltage booster coil or low-voltag starting vibrator.

The high-voltage booster coil of th vibratory type has superseded the moto or manually operated booster magnet This is a device for obtaining a high inte mittent voltage from a source of stead low voltage, such as a battery, accon plished by electromagnetic induction (se Figure 2). A primary winding of rel tively few turns and a secondary windi of many turns are wound on a commo

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transformers and rotating machines, is reviewed, and test methods are indicated.

Appendix

Since the preparation of this paper, the author's attention was called to the paper by L. V. Bewley and Hillel Poritsky,[1] wherein a rigorous analysis is applied to stray losses in the core. Their equation 38, giving the stray loss in watts per centimeter of transformer core, was found to be the same as equation 5 given in this paper. As the transformation may be of interest, it is as follows:

$$W=\frac{\lambda y}{24}\left(\frac{\omega\phi_o}{10^8}\right)^2\frac{a}{b}\ \text{watts per centimeter of core length} \quad (38)$$

where

λy = conductivity across the laminations
$\omega = 2\pi f$, with f in cycles per second
ϕ_o = total flux, peak value
a = lamination width
b = height of lamination stack

The factor a corresponds to W and b to aW, with $\phi_o = BKaW^2$. The weight of the core one centimeter is $aW^2Kd/453.6$ pounds. Substitution in equation 38 gives:

$$\frac{\text{Watts}}{\text{Pound}}=\frac{\lambda y 4\pi^2 f^2 B^2 W^2 K 453.6}{24 d 10^{16}} \quad (38a)$$

The conductivity λy is equal to t/r, and equation 38a becomes:

$$\frac{\text{Watts}}{\text{Pound}}=\frac{4\pi^2 f^2 B^2 W^2 K t 453.6}{24 d r 10^{10}} \quad (38b)$$

and with W and t in inches, equation 38b becomes

$$\frac{\text{Watts}}{\text{Pound}}=\frac{1.2224 f^2 B^2 W_a{}^2 K t_a}{r d 10^6} \quad (38c)$$

The constant 1.2224 of equation 38c about 0.1 of one per cent higher in valu than 1.2211, as obtained in equation 5.

Equation 38c yields the same interlamin tion resistance as is obtained from equatio 5, being based on the average stray lo rather than the desirable value obtaine from the maximum stray loss of equation Unfortunately, equation 38 does not call the designer's attention immediately n clearly that the interlamination resistance a function of the square of the laminatio width and the lamination thickness, a though it shows that the resistance is a fun tion of the square of the flux density and fr quency.

Reference

1. INTERSHEET EDDY-CURRENT LOSS IN LA NATED CORES, L. V. Bewley, Hillel Poritsk AIEE TRANSACTIONS, volume 56, 1937, Ma section, pages 344–6.

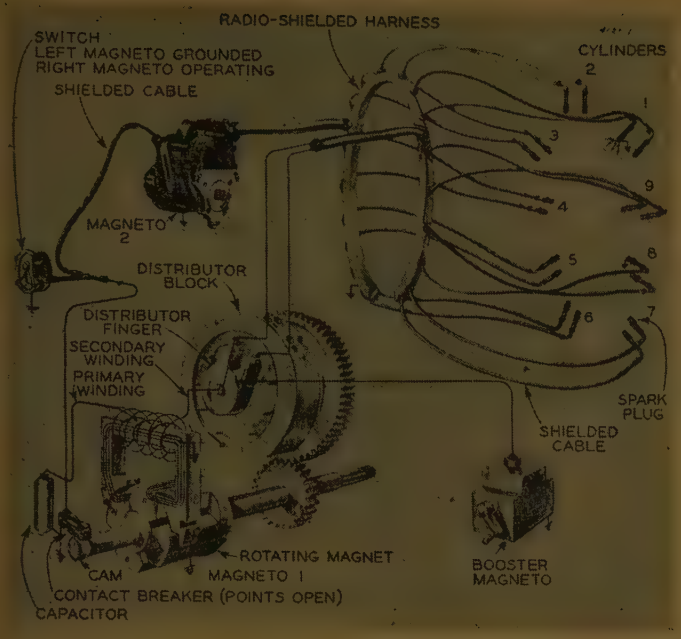


Figure 1. Schematic illustration of aircraft ignition system

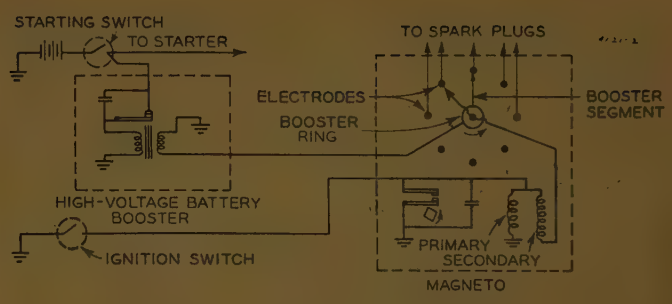


Figure 2. High-voltage battery booster circuit

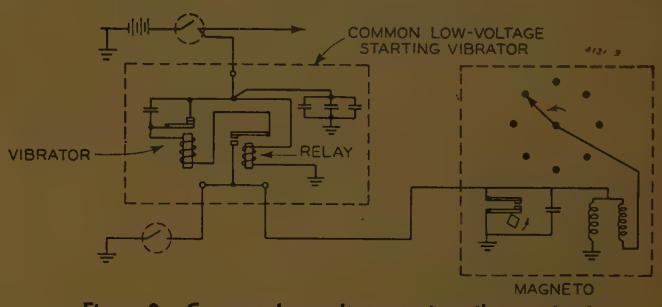


Figure 3. Common low-voltage starting vibrator circuit

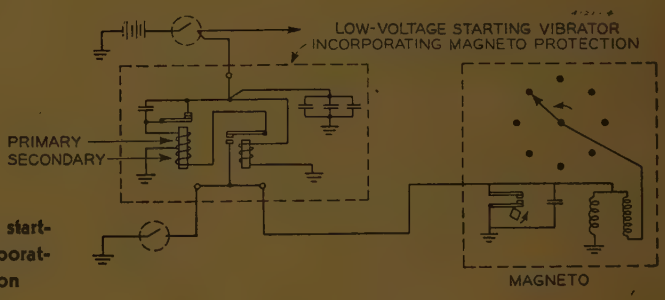


Figure 4. Low-voltage starting vibrator circuit incorporating magneto protection

laminated soft-iron core. A breaker mechanism is in series with the primary winding and battery for the purpose of producing rapid change of flux setting up high-voltage surges in the secondary winding. The secondary winding in turn connects to the magneto distributor segment.

The battery is connected to the booster coil through the engine-starter engaging switch on inertia starters or motor switch on direct cranking starters (see Figure 2). When the starter switch is closed, the booster coil is energized to provide high-voltage current at the spark plugs prior to coming-in speed. The booster coil fires but one plug in each cylinder; this is the plug giving the least detonation and is usually the front plug on radial engines. This plug usually is fired by the right magneto or the right side of a double magneto. Booster coils are not connected to both magnetos nor to both sides of a double magneto.

The main objection to high-voltage booster systems is the inherent disadvantage in that the trailing finger of the magneto must be placed so far back, to prevent jumping back at altitude, that the starting spark may occur anywhere from 30 to 60 degrees after top dead center. This difficulty has been overcome and correct timing of the starting spark obtained by boosting the main magnetos, thus dispensing with starter magnetos, or high-voltage coils, magneto booster rings and trailing segments. This method consists of using a separate induction coil (low-voltage starting vibrator) connected to the magneto primary winding, utilizing it as a transformer and supplying it with alternating impulses of the order of 150 volts (see Figure 3). It is made up of a coil wound on a soft-iron core connected in series with a breaker operated by magnetic action of the core. A relay is included, in series with and between the booster and magneto, to prevent magneto current feedback through the booster when the booster is not in operation.

A complete wiring diagram of a low-voltage starting vibrator is shown in Figure 3. A battery is connected to the magneto primary winding—through the breaker and vibrator coil. A circuit such as this has one drawback in that no protection is afforded the main magneto in instances when breaker contacts tend to "freeze." "Freezing" of breaker contacts results in high current flow, in some cases 40–45 amperes in a 24-volt system, due to low resistance value of vibrator and magneto windings. High current flow will cause demagnetization of magneto permanent magnets and burning of magneto primary winding. This difficulty has been overcome by using a system illustrated in Figure 4. The vibrator circuit here differs from the common unit in that the coil consists of a double winding having a common core and common ground. The primary winding has more turns than the secondary winding, so that, for a given magneto current requirement, a lesser amount need be interrupted through the primary winding. This is not the case in Figure 3. This feature contributes greatly to longer contact life, and, too, should the breaker contacts "freeze," no induction takes place, and current will flow through the primary winding to ground. Under these conditions no harm can be done to the magneto.

Earlier types of magnetos have magnets of low-coercive-force steel which would lose their magnetism if used with a low-voltage starting vibrator. Engine starting speed, hand cranking, kickback, and required spark retard for starting all have a bearing on use of low-voltage units in place of high-voltage type.

Oscillations in a vibratory system are very complex, involving the primary LC circuit, the secondary LC circuit and distributor-to-spark plug circuit. Calculations of a coupled circuit are difficult enough in an a-c case; they are more so in the transient case and practically impossible when spark gaps, nonlinear iron, and dielectric losses are combined with free oscillations.

Extending the Use of Shunt Capacitors by Means of Automatic Switching

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Synopsis: It generally is recognized that the kilowatt loading on distribution circuits or plant feeders may be increased substantially by using capacitors to supply the kilovar load and relieve the current-carrying system of this burden. The use of capacitors to boost voltage or actually to regulate voltage has increased during recent years. Automatic switching of capacitors makes it possible to obtain the benefits of adding capacitors to take care of the full load kilovars without the objection of excess capacitor kilovars at light loads. It also makes it possible not only to apply capacitors in the most effective manner from the standpoint of meeting power-factor limits established by the utility, but also to regulate voltage in the plant or on the system.

Capacitors may be switched automatically by the same switching means controlling the load, or they may be switched in response to changes in circuit conditions such as voltage, current flowing, or the reactive kilovars drawn by the load. The advantages of the various types of control and the choice of the number and size of capacitor steps to be switched are discussed fully.

THE wide acceptance of shunt capacitors and the desire to obtain the benefits of adding sufficient capacitor kilovars to meet the full-load kilovar requirements has resulted in a demand for equipment which will switch them automatically to avoid excessive leading kilovars or overvoltages at light loads. The automatic switching makes it possible to obtain the benefits of additional capacitor correction that otherwise would not be permitted. The effectiveness of shunt capacitors often can be increased by switching them "on" and "off" in accordance with changes in load. This is particularly true on circuits with widely varying loads where it is desired to maintain improved voltage conditions, or hold the current or kilovars supplied by the source to a minimum with variation in kilowatt loading. The demand for switching equipment increases as more and more capacitors are added to compensate, in whole or in part, for the kilovars demanded at full load. In general, capacitors should be switched to prevent overvoltages, to reduce line current, to keep the demand low in proportion to the kilowatt loading, or keep the over-all power factor within desired limits.

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When Should Capacitors Be Switched?

Overvoltage

Capacitors may be switched to avoid overvoltages on lamps, motors, and so forth at light load. Standard capacitors are designed for operation on circuits whose average voltage over a 24-hour period does not exceed the normal rated voltage by more than five per cent. The variations above the average may go up to the accepted standard limit of 115 per cent of normal rating for 230-, 460- and 575-volt capacitors or the standard limit of 110 per cent of normal rating for capacitors with higher voltage ratings.

Voltage Regulation

Switched capacitors can be located on distribution circuits or plant feeders beyond the voltage regulators to improve voltage regulation by reducing the current flowing in the feeders. This is accomplished without changing existing regulating equipment and lowers the IX and IR losses in the circuit.

Reduction of Power Costs

To maintain minimum cost of power purchased under certain rate schedules, capacitor kilovars should be varied to keep the power factor within certain limits or to keep the demand to a minimum with variation in kilowatt loading.

Avoid Objectionable Harmonics at Light Load

In a few cases it has been found desirable to switch capacitors "off" during light-load conditions because of the combination of objectionable harmonics and overvoltages. These cases are rare, but when they are encountered, it is usually more economical and satisfactory to switch the capacitors "off" automatically during light loads when they are not required than to apply a filter or wave trap.

The decision on whether or not the capacitors should be switched automatically depends upon the benefits expected, the size of the capacitor banks, the extent to which the kilowatt and kilovar load varies over a typical load period, and the variation of voltage with load. The first step in making an analysis usually is to make measurements of kilovars, kilowatts, and voltage over a typical load period to determine the capacitor kilovar requirements and then, if automatic switching is needed, to determine what type of control should be used for actuating the switching device.

Methods of Switching Capacitors

Capacitors may be switched manually with variations in load. Requiring an attendant to make the necessary observations of operating conditions such as voltage, power factor, kilovar demand, and so forth, this method of switching should be considered for those cases where an attendant can be assigned the specific duty of performing the switching operations.

In general, capacitors should be applied as near the load as possible. Therefore, if it is found desirable to switch capacitors with variations in load, one of the first things to consider is the distribution of the capacitors on the feeders and their connection directly to the motor terminals so that they may be switched "on" and "off" with the motor without additional switching equipment. In this connection, the capacitor kilovars switched with the motor switch should be selected to avoid excessive overvoltage due to self-excitation. It is difficult to determine how large the capacitor may be without causing excessive overvoltages unless the motor characteristic curves are available. The majority of motors will generate about 165 per cent normal rated voltage when disconnected from the line with a capacitor whose kilovar rating is equal to the motor horsepower rating. Some motors may generate higher voltages under these conditions. No overvoltage is encountered if the capacitor kilovar is not greater than the motor no-load kilovar.

In some instances it may be desirable to use a separate switch for the capacitor

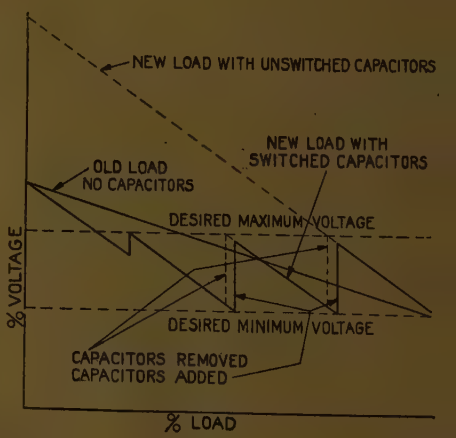


Figure 1. Kilowatt load increased 225 per cent by adding switched capacitors without increasing voltage regulation

which is controlled by the motor control. If the motors are small or if, due to load factors involved, it is not practical to switch the capacitors with the motors, then automatic switching equipment should be used with the capacitor.

If the load follows the same repetitive load pattern each week, then a time switch may be a practical means of initiating the switching operations. This type of device is not suitable for applications where the load fluctuates widely with a variable load cycle.

Capacitors also may be switched automatically in response to changes in load. There are three generally accepted methods of initiating the switching of capacitors automatically:

1. Voltage control—In response to changes in circuit voltage by means of a voltage-sensitive relay.

2. Current control—In response to changes in load current by means of a current-sensitive relay.

3. Kilovar control—In response to changes in reactive load by means of a kilovar-sensitive relay.

Voltage Control

The automatic switching of capacitors in response to changes in circuit voltage may be considered for applications where objectionable voltage deviations occur with varying loads. It is well known that capacitors will raise the general voltage level on a typical circuit.

To have a clear conception of voltage changes or voltage regulation, it will be helpful to remember that for all practical purposes the voltage drop can be determined from the kilowatt-load component flowing through the resistance of the circuit plus the kilovar component flowing through the reactance of the circuit. The voltage drop caused by the kilowatts is the product of the kilowatt load in amperes and the circuit resistance in ohms. The voltage drop caused by kilovars is the product of the kilovar load in amperes and the circuit reactance in ohms. The total voltage drop is the sum of the two. The voltage rise produced by the capacitor is the product of the circuit-reactance ohms and the change in kilovar-load amperes provided by the capacitor. The voltage change affected by switching a capacitor is the product of the circuit-reactance ohms and the change in kilovar-load amperes produced when the capacitor is switched. In calculation of voltage rise produced by the capacitor above an existing voltage level, the nonlinear variation of exciting kilovars drawn by motors, transformer, and so forth, should be given due consideration. If the voltage level is high, the exciting kilovar load may increase rapidly with increase in voltage and should be taken into account in arriving at the net kilovar load. It also should be remembered that the capacitor kilovar varies as the square of the voltage for a given rating. If the voltage level is moderate and steps are taken to permit only a small change in voltage, the nonlinear variation of exciting kilovars may be of no consequence and may be disregarded. In this case the voltage rise produced by the capacitor is the product of the circuit-reactance ohms and the capacitor current.

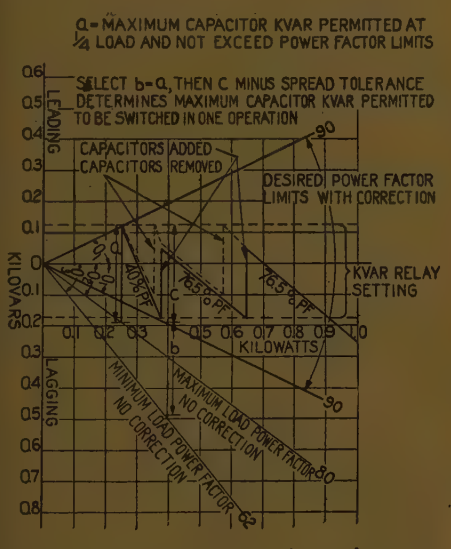


Figure 2. Curves showing relation between kilovars, kilowatts, and power factor, and how capacitors may be switched to keep power factor or kilovar demand within desired limits

The following steps are necessary to determine the amount of capacitors to be connected permanently and the number and size of capacitors to be switched to maintain satisfactory voltage conditions:

1. Determine light-load voltage with total capacitor kilovars connected.

2. If the light-load voltage with the total capacitor kilovars connected is objectionable, then determine the maximum capacitor kilovars permitted during light-load conditions without exceeding the upper voltage limit.

3. Determine maximum-size capacitor that can be switched in one operation or step without exceeding the permissible voltage change.

4. The total capacitor kilovars to be switched is the difference between the total capacitor kilovars required and the capacitor kilovars permitted during light-load conditions.

5. The minimum number of switching steps is the ratio of total capacitor kilovars to be switched to the maximum capacitor kilovars that can be switched at one operation without exceeding the permissible voltage change.

Example. Consider the case of an industrial plant at the end of a feeder where it is desired to increase the load from 442 kilowatts at 65 per cent power factor to 950 kilowatts at 65 per cent power factor without exceeding the current rating of the circuit or increasing the voltage regulation. Power is supplied from a substation over a 2,400-volt three-phase primary feeder consisting of 4/0 cable with equivalent spacing of 33.6 inches, to the 1,000-kva 2,400/460-volt plant transformer bank having 5.4 per cent reactance and 5.55 per cent impedance. The distance from the load center to the transformer bank is approximately 50 feet and from the transformer to the substation is one-half mile. The circuit impedance between the load and the substation as figured on a 2,400-volt base is as follows:

Substation to transformer	$0.15 + j0.315$
Transformer	$0.073 + j0.31$
Transformer to load center	$0.013 + j0.131$
Total	$0.236 + j0.756$ ohm

The kilovars permitted when supplying 950 kilowatts without exceeding the 1,000-kva rating of the plant transformer is 310 kilovars ($\sqrt{1{,}000^2 - 950^2} = 310$).

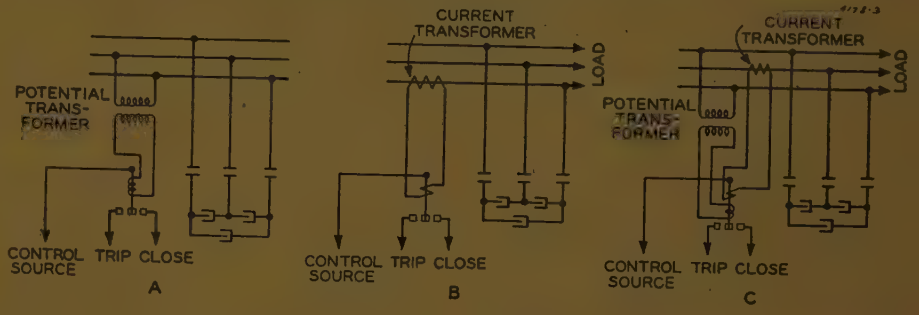


Figure 3. Three methods of automatically switching capacitors

A. Voltage control
B. Current control
C. Kilovar control

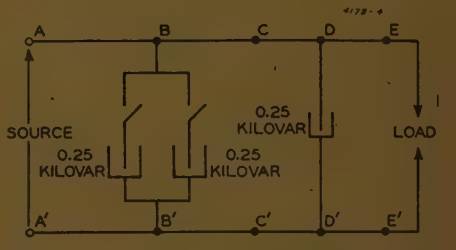


Figure 4. Circuit diagram showing location of load and capacitors referred to in Table I

Table I. Relation Between Power Factor, Kilowatts, Kilovars, and Kilovolt-Amperes for an Assumed Variation of Load Where Capacitors Are Located as Shown in Figure 4 and Switched at Predetermined Values of Load

Load Kilowatt	Capacitor Kilovar B—B′	Capacitor Kilovar D—D′	Conditions at E—E′ Power Factor	Conditions at E—E′ Kilovolt-Amperes	Conditions at E—E′ Kilovar	Conditions at C—C′ Power Factor	Conditions at C—C′ Kilovolt-Amperes	Conditions at C—C′ Kilovar	Load Increasing, Conditions at A—A′ Power Factor	Load Increasing, Conditions at A—A′ Kilovolt-Amperes	Load Increasing, Conditions at A—A′ Kilovar	Load Decreasing, Conditions at A—A′ Power Factor	Load Decreasing, Conditions at A—A′ Kilovolt-Amperes	Load Decreasing, Conditions at A—A′ Kilovar
1.0	0.50	0.25	0.707	1.415	1.0	0.80	1.25	0.75	0.97*	1.03	0.25*	0.97*	1.03	0.25
0.9	0.50	0.25	0.707	1.275	0.9	0.81	1.11	0.65	0.986	0.912	0.15*	0.986	0.91	0.15
0.9	0.50	0.25	0.707	1.13	0.8	0.824	0.972	0.55	0.998*	0.802	0.05*	0.998*	0.802	0.05
0.75	0.50	0.25	0.707	1.06	0.75	0.832	0.902	0.50	1.00	0.75	0.0	1.00	0.75	0.0
0.75	0.25	0.25	0.707	1.06	0.75	0.832	0.902	0.50	0.948*	0.792	0.25			
0.7	0.50	0.25	0.707	0.99	0.7	0.84	0.833	0.45				0.937#	0.702	0.05#
0.7	0.25	0.25	0.707	0.99	0.7	0.84	0.833	0.45	0.961*	0.728	0.20*			
0.65	0.50	0.25	0.707	0.92	0.65	0.85	0.764	0.40				0.988#	0.659	0.10#
0.65	0.25	0.25	0.707	0.92	0.65	0.85	0.764	0.40	0.974*	0.668	0.15*	0.974*	0.668	0.15*
0.6	0.25	0.25	0.707	0.85	0.6	0.863	0.96	0.35	0.986*	0.608	0.10*	0.986*	0.608	0.10*
0.5	0.25	0.25	0.707	0.708	0.5	0.893	0.56	0.25	1.00*	0.50	0.0	1.00	0.50	0.0
0.5	0.0	0.25	0.707	0.708	0.5	0.893	0.56	0.25	0.894*	0.56	0.25*			
0.45	0.25	0.25	0.707	0.637	0.45	0.913	0.494	0.20				0.993#	0.454	0.05#
0.45	0.0	0.25	0.707	0.637	0.45	0.913	0.494	0.20	0.913*	0.494	0.20*			
0.4	0.25	0.25	0.707	0.566	0.4	0.935	0.428	0.15				0.97#	0.413	0.10#
0.4	0.0	0.25	0.707	0.566	0.4	0.935	0.428	0.15	0.936*	0.427	0.15*	0.936*	0.427	0.15*
0.3	0.0	0.25	0.707	0.425	0.3	0.986	0.304	0.05	0.986*	0.304	0.05*	0.983*	0.304	0.05*
0.25	0.0	0.25	0.707	0.354	0.25	1.00	0.25	0.0	1.00	0.25	0.0	1.00*	0.25	0.0

* Lagging. # Leading.

The reactive component of the 950 kilowatt load at 65 per cent power factor is 1,110 kilovars (kvar = tan $\theta \times$ kw = $1.17 \times 950 = 1{,}110$). The expected load variation is from one fourth to full load or $237\frac{1}{2}$ to 950 kw. If the measured voltage at $237\frac{1}{2}$ kw is 101 per cent and the permissible voltage change is ± 2.6 per cent of normal then the size and number of capacitors can be determined, neglecting the nonlinear variation of exciting kilovars, from the following formula:

$$\text{Per cent voltage rise produced by capacitor} = \frac{\text{capacitor kilovars} \times \text{circuit reactance in ohms}}{10 \times (\text{line kilovolts})^2}$$

The 800-kilovar capacitor at one fourth load will raise the voltage approximately $10\frac{1}{2}$ per cent $(800 \times 0.756)/10 \times 2.4^2 = 10\frac{1}{2}$, which is excessively high. Since the voltage is one per cent above the mean level without capacitors at one fourth load, then the maximum capacitor kilovar permitted should not increase the voltage more than 1.6 per cent (2.6 − 1.0). The capacitor permitted at one fourth load is $1.6 \times 2.4^2 \times 10/0.756 = 122$ kilovars. Use the nearest standard rating of 120 kilovars.

Since it is desired to keep the voltage within ± 2.6 per cent of normal, the capacitor selected should not change the voltage by more than 5.2 per cent minus a margin or tolerance which will prevent the possibility of hunting. If a margin of 0.3 per cent is chosen at both the upper and lower limits, then the capacitor kilovar selected should not change the voltage more than 4.6 per cent. The maximum size capacitor to be switched in one step is 350 kilovars $(4.6 \times 2.4^2 \times 10)/0.756 = 350$, and the minimum number of switching steps is two since $(800 - 120)/350 = 1.94$.

Figure 1 shows graphically the voltage regulation with variation in load for the old load without capacitors and the new load with unswitched and with switched capacitors. The 120-kilovar capacitor representing 15 per cent of the total is permanently connected to give a voltage rise of approximately 1.6 per cent at one fourth load. The remaining 680 kilovars is divided into two equal groups of 340 kilovars and switched automatically in response to voltage to keep the voltage within the desired limits. Each 340-kilovar capacitor switched changes the voltage approximately 4.5 per cent.

Kilovar Versus Current Control

When capacitors are connected to a well-regulated bus, the voltage regulator or other devices usually will keep the voltage within satisfactory limits or attempt to do so independently of the amount of capacitor kilovars applied. Circuit voltage, therefore, does not reflect truly the need for capacitors and, if the capacitors are to be switched, other methods of detecting the capacitor requirements should be used.

If sufficient capacitor kilovars are added to supply the lagging reactive kilovars at full load, then it may be desirable to switch the capacitors with variation in load to obtain maximum benefits by keeping the demand and losses to a minimum or to avoid the possible penalty of excessive leading power factor during light loads. In some instances the kilovars supplied by the capacitor make it possible for the regulating equipment to keep the voltage up to a satisfactory level at full load and in such cases the voltage at light load is very likely to be excessive unless capacitors are switched.

One method to consider is that of switching the capacitors in relation to line current. This is satisfactory and preferred provided the load power factor remains relatively constant with variation in kilowatt loading or the power factor of the load varies in a predictable manner with a variation in kilowatt loading. However, if the load power factor does not vary in a predictable manner, then the capacitor should be switched in response to the load kilovar requirements. When the latter type of control is used, it is not necessary for the load power factor to have a definite relationship with kilowatt loading. In cases where either type of control can be used, the existing physical circuit arrangement may dictate the type to be selected.

Kilovar Control

The kilovar method of control should be used to switch capacitors when the load voltage is regulated and the load power factor varies in an unpredictable manner with variation in kilowatt loading. It often may be used in conjunction with existing voltage regulators to improve voltage regulation. In some instances the existing voltage regulators may not provide satisfactory voltage regulation but when switched capacitors are added to supply the kilovars of the load, the regulators then may provide satisfactory regulation.

When the kilovar type of control is used, the following steps are suggested for determining the amount of capacitors to be connected permanently and the number and size of capacitors to be switched:

1. Determine the capacitor kilovars required at light load to maintain desired power factor or to keep kilovar demand within desirable limits.

2. Determine the maximum capacitor kilovars that can be switched in one operation without exceeding the permissible change in power factor or kilovar demand.

3. The difference between the total capacitor kilovars proposed and the capacitor kilovars permitted duirng light-load conditions determines the total capacitor kilovars to be switched.

4. The minimum number of switching steps is the ratio of the total capacitor kilovars to be switched to the maximum kilovars that can be switched in one operation and stay within the power-factor limits or kilovar-demand limits.

Example. Consider the case of an industrial plant where it is desired to improve the full-load power factor from 70.7 per cent to 97 per cent and thereby reduce the demand approximately 27 per cent or from 1.41 kva per kilowatt to 1.03 kva per kilowatt. The capacitor required to do this is 0.75 kilovar per kilowatt. The expected kilowatt-load variation is between one-fourth and full load and the maximum variation of load power factor without correction is between 62 per cent and 80 per cent lagging. If it is desired to add correction to keep the power factor between 90 per cent lagging and 90 per cent leading for all load conditions, then the boundaries are outlined which will permit the selection of maximum capacitor kilovar at light load.

The kilovars at light load and at maximum uncorrected power factor plus the leading kilovars permitted at light load without exceeding the permissible leading power factor determines the maximum capacitor kilovars permitted at light load or $0.187+0.12=0.309$ kilovar. Once the permanently connected capacitor is determined, this together with the previous boundaries permits the selection of the maximum capacitor kilovars that may be switched automatically in one operation. If 0.3 capacitor kilovar is connected permanently, determine the kilowatt loading at which the 0.3 capacitor kilovar will improve the power factor from 62 per cent to 90 per cent. See Figure 2, line $b=0.3$ kilovar at 0.377 kw. The capacitor kilovars required to improve the power factor at this loading point from 90 per cent to unity plus the leading kilovars permitted at light load is 0.305 kilovar. See Figure 2, line *c*. This determines the maximum capacitor kilovars that can be switched and stay within the power-factor limits. Since the microfarad tolerances of shunt capacitors are usually in the plus direction, and also since the kilovars vary as the square of the voltage, the kilovar relay should always be adjusted for a greater spread than the kilovar rating of the capacitor by a margin sufficient to avoid hunting. If the capacitor kilovars are reduced to 75 per cent of its original calculated value to provide this margin, then the normal capacitor rating should not exceed 0.229 kilovar. The minimum number of automatically switched capacitor steps is $(0.75-0.3)/0.229=1.94$, therefore, the 0.45 capacitor kilovar should be divided into two equal groups of 0.225 kilovar. The kilovar relay may be adjusted to switch the capacitors "on" at 0.175-kilovar lagging with increase in loading and switch capacitors "off" at 0.125-kilovar leading with decrease in loading.

The curves in Figure 2 show power-factor boundaries with and without correction. The two broken horizontal lines show the kilovar relay settings for switching the capacitors "on" and "off" to keep the power factor at all times within 90 per cent lead or lag for load variation of one-fourth to full load. For the one particular variation of loading illustrated, the permanently connected 0.3-kilovar capacitor changed the power factor at one-fourth load from 80 per cent lag to 90 per cent lead. The load increases at a minimum power-factor slope of 40 per cent until the first automatically switched capacitor is added when the reactive reaches 0.175 kilovar. The 0.225-kilovar capacitor changes the power factor to slightly leading and the load increases at a power-factor slope of $76\frac{1}{2}$ per cent until the second automatically switched capacitor is added when the kilovar reaches 0.175. The second 0.225-kilovar capacitor changes the power factor to slightly leading and the load increases at a power-factor slope of $76\frac{1}{2}$ per cent until full-load conditions are reached. On decrease of load the capacitors are switched "off" in the reverse order when the leading reactive reaches 0.125 kilovar as indicated by the broken extended lines. The loading may increase or decrease at different power-factor slopes than those shown and the power factor will be maintained within the desired limits so long as the operating conditions are within the boundaries outlined.

If the full load in this case happened to be 1,000 kw, then a total of 750 capacitor kilovars is required, 300 kilovars to be connected permanently and the remaining 450 kilovars to be switched in two equal steps of 225 kilovars.



Figure 5. Single-step kilovar control assembled with outdoor capacitor rack showing kilovar relay, two-element time-delay relay, and control switch

Current Control

Current control should be considered for switching capacitors where the voltage is well regulated and the power factor of the load remains substantially constant with variation in kilowatt loading or if the power factor of the circuit varies in a predictable manner with variation in kilowatt loading. If in some instance the existing voltage regulators do not give satisfactory voltage regulation, capacitors may be added and switched in response to current to obtain satisfactory voltage regulation in combination with the regulators.

In analyzing the application of capacitors automatically switched in response to current there are always at least two power factors involved and in some instances more. To illustrate how some of the various circuit conditions should be analyzed when more than two power factors are involved, a diagram and tabulation has been prepared. The values shown in Table I are based on the following assumptions:

1. The power factor at point *E–E* remains at 70.7 per cent lagging for the maximum load variation of 25 to 100 per cent.

2. One 0.25-kilovar unswitched capacitor is located at point *D–D* to improve the power factor at *E–E* to unity at minimum load of 25 per cent.

3. Two 0.25-kilovar switched capacitors are located at point *B–B* to improve the circuit power factor at *E–E* to 97 per cent lagging at full load. One capacitor to be switched on at 50 per cent kw load and off at 40 per cent kw load and the second capacitor to be switched on at 75 per cent kw load and off at 65 per cent kw load.

The number and size of the capacitor can be determined in a similar manner to that described for the kilovar control. If more than one switched capacitor is required, a separate current control is required for each capacitor switch. These can be adjusted for different current values to give the same results as multistep kilovar control.

Equipment Required

The automatically switched capacitor station consists essentially of the capacitor, the capacitor switch, the automatic control and instrument transformers, and control source.

The Capacitor

The capacitor usually will be one of the standard ratings and types of mounting



Figure 6. A 1,440-kilovar three-phase 460-volt capacitor divided into four 360-kilovar indoor equipments with electrically operated air circuit breakers and four-step automatic control

which is suitable for the particular location and circuit requirements.

Capacitor Switch

The capacitor switch should be equipped with overcurrent trip devices unless other overcurrent protection is provided. If the switch is equipped with overload and fault-current protection, then it should also have a lockout feature to prevent the switch from closing, if tripped from overcurrent, until the lockout device is reset manually. Of course, if the switch does not have overcurrent trip devices, then no lockout feature is required and the master breaker or line fuses should provide the overcurrent protection.

The capacitor switch should be suitable for repetitive duty and should be selected to have the correct rating as to voltage, continuous current, momentary current, and interrupting ability. Contactors with line fuses or overload attachments may be considered for switching small low-voltage capacitors where the fault current and momentary inrush current does not exceed the maximum momentary rating of the contactor. Oil-immersed contactors may be used on small high-voltage capacitors on a similar basis.

Electrically operated circuit breakers generally are used for switching the larger size capacitor equipments. The breaker should, of course, be selected to have the proper voltage, continuous current, momentary current, and interrupting ability. On the very large banks of the higher voltage ratings the possibility of restriking when opening the circuit to the capacitor is greater and this should be taken into consideration when selecting a breaker.

It is often necessary to have current transformers in the circuit to the capacitors as a means of energizing breaker overcurrent trips. If the transient inrush current is high when the capacitor switch is closed, then sufficiently high voltage may appear across the secondary winding to necessitate a protective device to limit the voltage to a safe value and avoid damaging the insulation in the secondary of the current transformer circuit. This is particularly true on large capacitor banks closely coupled.

Automatic Control

The automatic control for switching capacitors usually consists of a master element, time-delay relays, auxiliary devices, and control switch. A voltage-sensitive relay, current-sensitive relay, or kilovar-sensitive relay may be selected as the master element for initiating the switching operations. The time-delay relays will allow sufficient time to prevent unnecessary switching operations, caused by momentary disturbances on the power circuit. The time-delay desired usually is not critical, but in the interest of keeping the transient current to a minimum, sufficient time delay should be chosen between the opening and closing of the capacitor switch to allow the discharge device to drain off a substantial part of the stored energy before the next switching operation. The auxiliary relays respond to the request of the master element and time-delay relay combination to close or open the capacitor switch. Control switches usually are desired to permit opening and closing of each capacitor switch by manual operation of its control switch or have the capacitor switch open and close in response to the master element.

Instrument Transformers and Control Source

A potential transformer is required for voltage control, a current transformer is required for current control and both current and potential transformers are required for kilovar control. See figures 3A, 3B, and 3C for their positions in circuit. In all cases a single-phase control source is required. Suitable instrument transformers and control source may or may not be available at the proposed capacitor location.

Description of Operation

Single-Step Control

If the control switch is in the automatic position and one set of the master-element contacts close, indicating that capacitor kilovars are required, this will energize the closing element of the time-delay relay. If the initial cause is not a temporary disturbance, then the master-element contacts will remain closed until the time-delay relay contacts have closed energizing the closing mechanism of the capacitor switch. The capacitor switch will then close and connect the capacitor to the circuit.

If with the capacitor connected to the circuit one set of the master-element contacts close, indicating that the capacitor correction should be removed, this energizes the tripping element of the time-delay relay. If the master-element contacts remain closed for the time required for the time-delay tripping contacts to close, then the capacitor-switch tripping device will be energized opening the capacitor switch and disconnecting the capacitor from the circuit.

Multistep Control

The multistep control operates in a manner similar to the single-step control except that it is arranged to switch two or more groups of capacitors selectively and automatically by means of a time-delay relay for each separate group in combination with auxiliary contacts on the capacitor switches. The time-delay relays operating in conjunction with the auxiliaries of the capacitor switches set up the circuits to close or open the capacitor switches in a definite sequence responding to the master element with time delay between switching operations.

Conclusions

1. The usefulness of shunt capacitors can be increased by switching them automatically.

2. The benefits of adding capacitors to supply kilovars up to full-load conditions can be obtained, if automatic switching is applied to remove the objectionable leading kilovars at light load.

3. The type of control to use and the size and number of automatically switched capacitors steps required to do a particular job can be determined when the operating conditions and requirements are known.

References

1. Tests and Analysis of Circuit-Breaker Performance When Switching Large Capacitor Banks, T. W. Schroeder, E. W. Boehne, J. W. Butler. AIEE Transactions, volume 61, 1942, November section, pages 821–31.

2. Safe Capacitor Selection for Power-Factor Improvement, J. E. Barkle. *Power*, April 1943.

Magnesyn Remote Indication

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REMOTE indication is, in general, the transfer of information or intelligence from one point to a remote point in a semipermanent form which may be visually observed. A variety of means can and have been used to accomplish such a purpose. Information can be sent from one place to another simply by moving a long string or cable, rotating a long flexible shaft, changing pressure in a hydraulic or pneumatic line, changing the characteristics of an electric circuit, as well as by methods involving thermal and optical devices. The fields of telemetering and visual recording are well-known branches of remote indication. Automatic control systems and computing mechanisms depend on remote indication for their source of intelligence.

As aircraft have increased in size and complexity, improved methods of remote indication have become most essential. Practically all information concerning the performance and conditions of engines and ship that is brought to the attention of the pilot comes to his instrument panel by means of remote indication. Relatively large distances exist between pilot and flight-instrument sensing elements, between flight engineer and engines, and between navigator and compass. Pneumatic and hydraulic methods are still most efficient for some functions, but a majority of indications are carried out electrically on modern aircraft. In many cases electric systems can offer a saving in weight, simplicity of installation and maintenance, greater accuracy, better interchangeability of components, reduction in military combat hazard, and a somewhat more inexpensive unit. In some cases electric systems have been the only practical way of remotely indicating a particular function.

Electrical methods of remote indication, as used on aircraft, can be divided into four general groups. D-c systems, using a variable resistance for a transmitter and a voltmeter for an indicator, were probably the first form to be used. This method is still widely used for temperature indication where the variable-resistance transmitter becomes a temperature "bulb," utilizing the inherent change in resistance with temperature of an electric conductor.

Autosyns* have been extensively used on aircraft since 1932 as an a-c electric remote-indicating system similar to the self-synchronous motors used in marine order signal systems and many industrial applications. A transmitting Autosyn which has a rotor mechanically actuated by the function to be indicated is electrically wired to one or more similar Autosyns as indicators with pointers and dials. Every motion of the transmitting rotor is reproduced as a motion of the indicating pointer. The prime mover may be any of the engine-pressure functions, liquid-level functions, or position functions of any movable part on the aircraft.

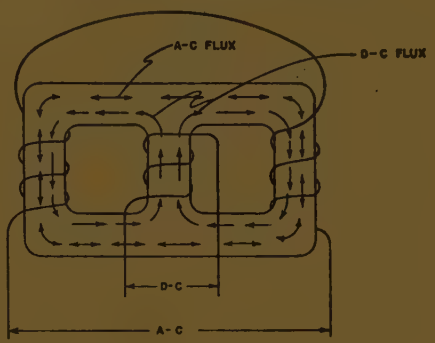


Figure 1. Frequency doubler

A third class of remote indicating instruments are the d-c Autosyns. Operation is analogous to the a-c Autosyn, where the a-c field is replaced by a d-c field.. The transmitter is a continuous circular resistance with three equally placed taps contacted on a diameter by a direct voltage. When connected to an indicator similarly wound with a continuous tapped winding on a magnetic iron core, a d-c field will be set up across a diameter of the indicator corresponding to the diameter of the transmitter that is contacted by the d-c voltage. The rotor of the indicator can then be a permanent magnet which will align itself with the indicator field.

A more recent development, which has become well suited to the requirements of aircraft remote indication, is the Magnesyn system. Originally applied to the problem of remote compass operation, it is equally adaptable to most of the functions which are remotely indicated on aircraft. It retains the advantages of a simple indicating unit, as used in the d-c Autosyn system, but utilizes a transmitting unit that is equally simple. The Magnesyn system is a-c operated and uses identical units for transmitter and receiver. In each case, the stator is a toroidally wound tapped coil on a laminated magnetic core, and the rotor is a permanent magnet. Rotation of the transmitter rotor causes corresponding rotation of the indicator rotor.

The preceding four systems are the only ones that have been used in quantity on aircraft. Other electric remote-indicating systems that have been considered are the distometer and the inductance bridge.

In order to understand the somewhat involved magnetic circuit of the Magnesyn, it is perhaps worth while to consider some of the chronological steps in its development. A well-known phenomenon basic to the operation of the Magnesyn is the production of harmonic voltages in a saturated reactor. Such a reactor, shown in Figure 1, is often used as a frequency doubler. An *E-I* lamination core of a magnetic material that saturates abruptly is wound with coils on the outside legs, series aiding, and a separate coil on the center leg. If a sufficiently large alternating current is passed through the outside windings, the magnetic path made up of the outside legs and top and bottom sections will be alternately saturated and unsaturated with magnetic flux twice every cycle. When direct current is passed through the center winding, the magnetic path presented to its field is made up of the two outside legs in parallel. Consequently the magnetic path of the d-c circuit is varying in permeability twice for each cycle of alternating current on the outside legs. This means that the flux passing through the d-c leg will vary from a maximum to a minimum twice for each a-c cycle, inducing a voltage in the d-c winding at even harmonic frequencies of the exciting voltage. The a-c circuit on the outer legs is acting as a "gate" on the amount of flux produced by the d-c field.

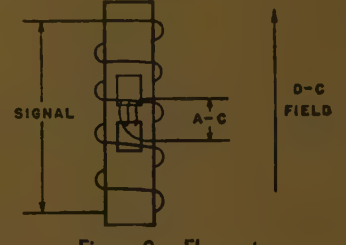


Figure 2. Flux gate

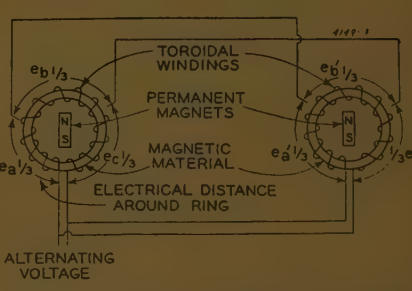


Figure 3. Magnesyn transmitter and receiver

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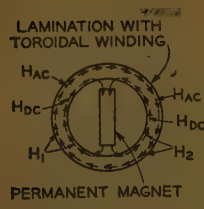


Figure 4 (above). Magnesyn magnetic circuit

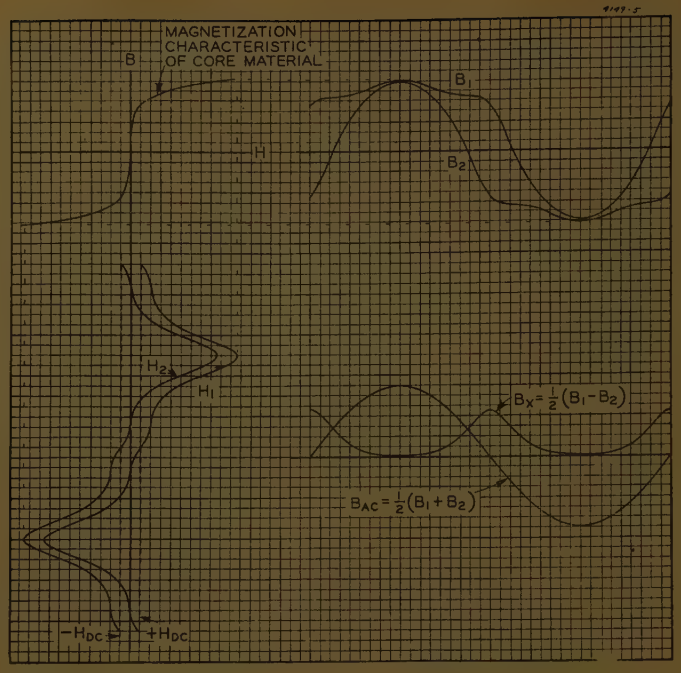


Figure 5 (right). Flux densities and corresponding magnetizing force

By replacing the field from the d-c winding by a permanent-magnet field, or even by the earth's magnetic field, the same principle of operation will apply. Early forms of a unit that operated in the earth's field were called "flux gates".* Construction of this form of frequency doubler is shown in Figure 2. An a-c winding on a portion of a strip of magnetic material serves to saturate the metal twice every cycle, thereby providing a path of variable permeability. Flux produced in the strip by the earth's field will then vary twice each cycle with the varying permeability, which, in turn, will produce an even harmonic voltage in a second winding around the strip. This action is basically the same as that of the original frequency doubler.

A further step in development was the idea that if fundamental flux in combination with a unidirectional field would produce an even harmonic flux in the aforementioned devices, then a fundamental flux in combination with even harmonic flux might be expected to produce a unidirectional field. Such proved to be the case and resulted in the Magnesyn system. Figure 3 shows the elements required for a Magnesyn transmitter and receiver or indicator. Both units can be identical electrically and magnetically. The stator of each is a circular core of magnetic material around which is placed a single continuous toroidal winding which is connected to a source of a-c voltage. Each winding is divided by two taps into three equal sections, and corresponding taps of the two units are connected together. The rotors are two-pole permanent magnets, magnetized along a line perpendicular to their axis of rotation, and are free to revolve in the centers of their respective stators. The Magnesyn is again a frequency doubler supplied by a unidirectional field from its rotor and presenting a variable-permeability path to this field because of the a-c flux produced by the winding.

By first considering the magnetic circuit of the transmitter, as shown in Figure 4, some of the relationships necessary for the production of even-harmonic flux in the stator can be seen. A magnetomotive force, H_{d-c}, will be set up in each half of the annular core by the presence of the permanent-magnet rotor. Assuming the core to be a homogeneous material, symmetrical in shape and concentric to the magnet, the reluctance of the two paths will be the same and H_{d-c} will be equal in the two halves of the ring.

When the transmitter winding is connected to a source of sine wave alternating voltage, the current drawn will produce an alternating magnetomotive force, H_{a-c}, which will be considered at a specific time to have a clockwise direction in the core. With respect to this exciting magnetomotive force, H_{a-c}, the unidirectional magnetomotive force is positive in one half of the core and negative in the other half. Then the total magnetomotive force in one half is

$$H_1 = H_{a-c} + H_{d-c} \quad (1)$$

and in the other half is

$$H_2 = H_{a-c} - H_{d-c} \quad (2)$$

A typical relationship between flux density produced in the core and magnetomotive force set up in the core is shown as the magnetization curve in Figure 5. By projecting a time cycle of H_1 and H_2 on this characteristic, the corresponding time cycles of flux densities B_1 and B_2 in each half of the core may be obtained. Flux density B_1 is made up of two components, B_{a-c} produced by the exciting voltages which is the same in both halves of the core, and an additional flux density B_x which exists because of the presence of H_{d-c}, combined with H_{a-c}, or

$$B_1 = B_{a-c} + B_x \quad (3)$$

Likewise, the flux density in the second half of the core

$$B_2 = B_{a-c} - B_x \quad (4)$$

From equations 3 and 4

$$B_{a-c} = {}^1/_2(B_1 + B_2) \quad (5)$$

and

$$B_x = {}^1/_2(B_1 - B_2) \quad (6)$$

The time relationships of B_{a-c} and B_x are shown in Figure 6, where it is clear that B_x is an even-harmonic function of B_{a-c} with a major second-harmonic component. Because voltage is induced in a winding surrounding a piece of magnetic material proportional to the rate of change of flux density in the material, component voltages E_{a-c} and E_x will be induced in the transmitter stator winding by B_{a-c} and B_x as shown in Figure 6. Total voltage induced by the axis of the magnet poles on the rotor then will be

$$E_1 = E_{a-c} + E_x \quad (7)$$

and in the other half will be

$$E_2 = E_{a-c} - E_x \quad (8)$$

The sum of E_1 and E_2 or $2E_{a-c}$ is equal to the impressed a-c voltage.

If we refer to Figure 3, the proportion of even-harmonic voltage appearing in each of the three sections of winding will depend on the position of the rotor. Figure 7 shows the relative amounts of this component voltage in each section as the magnet is rotated through 360 degrees. The fundamental component of voltage in each section, of course, is independent of magnet position and is equal to one third of the total applied voltage.

If taps on the transmitter are connected to the corresponding taps on the receiver stator, omitting the receiver rotor magnet for the moment, and both coils are sup-

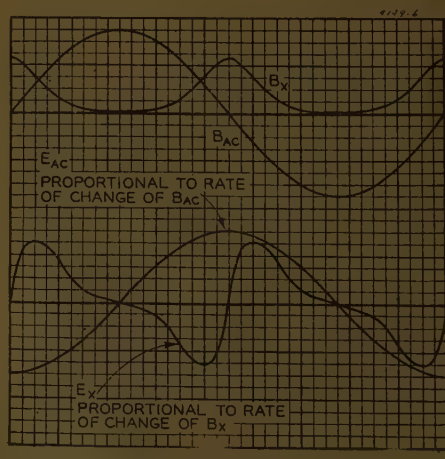


Figure 6. Flux density and voltages in the Magnesyn

* Flux Gate is a registered trade-mark of Bendix Aviation Corporation.

plied from the same a-c source, there will be no flow of fundamental current between the two units, because the voltages at the taps of each are at the same fundamental potentials. However, because there are even-harmonic voltages present in each section of the transmitter but none in a receiver with no magnet, there will be a flow of even-harmonic currents between the two coils. This flow of current effectively lowers E_x at the transmitter to some value, E_x', at each instant of time, and produces the same voltage, E_x', in the receiver winding in a corresponding position with respect to the three sections of winding.

It is difficult to obtain the actual value of E_x', but the action of the Magnesyn system can be none the less faithfully followed if E_x' is considered to be the same as E_x, which is the same as saying that B_1 and B_2 in the transmitter core are unchanged when the receiver stator is connected. Because the voltages in the receiver-stator winding have to be the same as those in the transmitter winding as a result of their interconnection, the flux densities in the receiver core must have the same rates of change. This means that B_1' and B_2' in the receiver must have the same time-cycle wave shape as B_1 and B_2 in the transmitter. Figure 8 shows B_1 in the transmitter with its associated H_1 followed by an assumed B_1' in the receiver with a time integral of zero and the H_1' in the receiver that would be required to produce the assumed B_1'. Such an assumption obviously presumes the presence of an effective unidirectional magnetomotive force in the receiver core which is impossible, inasmuch as there is no source of direct current anywhere in the system, nor is there any unidirectional field applied at the receiver. This delimits B_1' in the receiver to such a value that the time integral of H_1' in the receiver is zero, which in turn requires an effective unidirectional component of flux density to be produced in one half of the receiver core. A similar argument will show that a corresponding unidirectional flux-density component exists in the other half of the receiver core. Therefore, an effective unidirectional field will exist across a diameter of the receiver core, corresponding to the diameter in the transmitter in line with the rotor magnet poles. As the

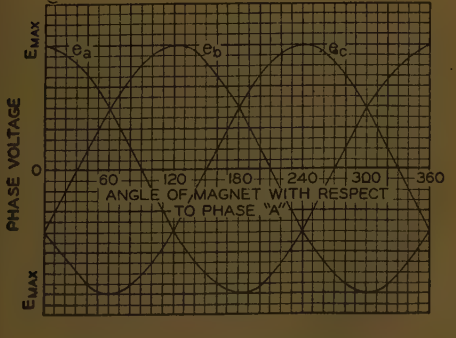


Figure 7. Phase-voltage variation

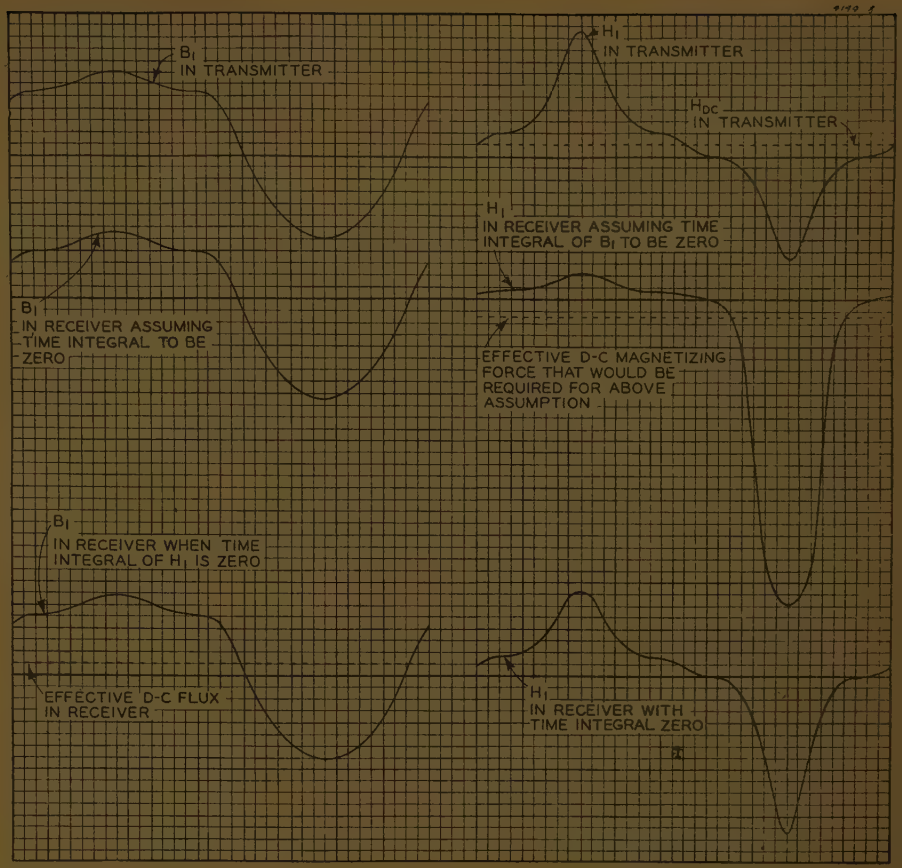


Figure 8. Development of poles in the Magnesyn indicator

transmitter rotor is turned, the effective unidirectional field in the receiver stator will turn likewise.

If a permanent-magnet rotor is introduced at the receiver, a restoring torque gradient will make it align itself in the unidirectional field at the receiver. When it is in line with this field, exactly the same even-harmonic voltages will be induced in both transmitter and receiver coils, and the flow of even harmonic currents will cease. Rotation of the magnet in one unit then will cause corresponding rotation of the magnet in the other unit, and, since they are identical, either unit can act as a transmitter.

Because of the nature of the parts involved, the Magnesyn is an inherently simple device to fabricate. All laminations can be annular-ring punchings from sheet metal. The windings are readily adapted to an automatic machine operation. A round disk magnet is used for the rotor and the entire rotor assembly is no more than this magnet pressed on a suitable shaft. In practice, an additional annular core of magnetic material is placed around the outside of the stator serving the dual functions of a return path for even-harmonic flux to the stator and a magnetic shield against extraneous magnetic fields that may be present near the unit.

Perhaps the most outstanding feature of the Magnesyn system is the extremely low reaction torque on a prime mover. In any system the torque gradient of the prime mover must be sufficiently large to mask any friction or error torque gradients in either transmitter or receiver. Any friction torque of brushes or sliding contacts, as well as the bearing friction of rotor weight, must be overcome. Magnesyns have no source of friction except bearings and with jeweled pivots supporting a total rotor weight of the order of one gram, this source of friction is negligible to the most sensitive prime movers. Error torques likewise can be kept to a low value without excessively involved assembly techniques.

Some applications of remote indication in aircraft require the transmitting unit to be installed at a point of extremely high vibration. The construction of a Magnesyn can be modified to make it admirably suited to this requirement. The stator, associated lead wires, and electric connector can be molded literally into one solid unit. The rotor can be mounted on a rugged shaft, with suitable bearings to withstand anticipated vibration. It is even possible to mount the magnet on the prime mover directly and mount a molded Magnesyn stator concentrically to the prime-mover shaft. Such units have been constructed that are waterproof, dirtproof, and oilproof, and that withstand temperatures from −100 to +200 degrees centigrade, with sustained vibration of 100 gravity in any direction. There is no possibility of wear external to the prime mover and no hazard of sparking contacts in an explosive atmosphere.

For certain applications, it is not necessary to have the transmitters and indicators physically the same. This permits remote indication of any moving member by placing a magnet on it and providing a stator at a reasonable distance away in the most convenient part of the assembly.

Some general characteristics of the system are freedom from errors resulting from voltage variation and temperature changes. Accuracy can be maintained at any desired level with suitable choice of transmitter components. Transmitters can be constructed to drive any number of receivers. Units for aircraft use are operated at 26 volts from a 400-cycle source and draw about 1.5 volt-amperes. When operated from an inverter, these Magnesyns will perform satisfactorily on frequencies between 150 and 1,200 cycles, provided the ratio of frequency to voltage is maintained between 13 and 17.

The most widespread use of the Magnesyn to date has been in remote-indicating compasses. In modern combat aircraft it has been essential to place the compass element in a location free from the magnetic effects of armor plate, machine guns, turrets, engines, and other interfering components, and bring the compass heading to the pilot and navigator by remote indication. A single magnet mounted in a compass float serves both the purposes of aligning itself in the earth's field and acting as the rotor of a Magnesyn transmitter. Below the liquid cavity containing the compass float is a Magnesyn stator. This stator is connected to a receiver, and the compass heading is reproduced by a pointer on a dial. No moving parts are added to a conventional magnetic compass.

A class of flight instruments, including air speed, rate of climb, and altitude, has been successfully remotely indicated with the Magnesyn system. These prime movers, in general, are too weak to operate other systems. Practical trailing "bombs" containing these flight-sensing elements have used Magnesyn indication. Remotely operated weather instruments have incorporated the Magnesyn system to advantage.

For indication of the position of various moving members, such as wheels, a variety of flaps, bomb-bay doors, and so on, the shockproof construction type of Magnesyn is well suited. Vibration, dirt, water, and ice have little effect on the molded unit. A particular application where the Magnesyn has stood up remarkably well, is the indication of propeller pitch. Here the position transmitter is mounted in the hub of a propeller and subjected to extreme vibration.

Fuel-level and fuel-flow instrumentation in general presents the requirement that the electric system be external to any cavity containing gasoline vapors as a safety precaution. Most systems accomplish this by having the prime mover in the gasoline drive a magnet which, in turn, drives a follower or slave magnet outside the compartment. By using a Magnesyn stator in place of the slave magnet, the transmitter is complete with no additional equipment.

The development, method of operation, and some features of the Magnesyn have been described with a comparison of other existing electric systems. Magnesyns can be used in a variety of remote-indicating devices, some of which were impractical previously. Future developments will depend on new requirements in the aircraft industry and potential applications in industrial instrumentation, as well as the ever-present problem of continuous improvement in existing equipment.

Auxiliary-Power-Plant Requirements

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Synopsis: Designers of electrical equipment for aircraft are confronted with many limitations. Some of these limitations may be nullified with the use of auxiliary power plants.

To cover various phases of application a series of engine–generators would be necessary. A large number of their design features should be adapted to each type of airplane in order to save weight and obtain best performance. This necessitates close co-operation between designers of the component parts of the airplane.

IN developing and extending the use of electric equipment for aircraft, designers have been faced with the eternal problems of space and weight limitations. Two places where a great many limitations have been encountered are at generator and battery.

In most cases generators are mounted on accessory sections of main engines. Diameter is limited by the presence of other accessories, and overhang is limited by the strength of the mounting flange. Another undesirable feature, which has precluded the use of alternators, is the wide speed range of the drive.

Batteries, as a source of auxiliary power, have been a millstone around the necks of designers. With the airplane on the ground the accessory system is almost useless unless ground power units are available. Radio and other accessories can be operated only momentarily without reducing the battery charge to the extent that the main engines cannot be started.

Generator designers have done an excellent job in recent years. They have succeeded in multiplying the output of generators several times without exceeding the rigid space and drive limitations. Although we may expect further development, it is not likely that it will be so rapid.

Batteries also have been improved; however the fundamental shortcoming which characterizes them cannot be overcome. They cannot deliver power beyond their ability to store it. It would be impractical to carry sufficient batteries for supplying auxiliary power in warm weather and impossible in cold weather.

Elimination of some or all of these stumbling blocks will enable electrical engineers to exploit further the use of electricity throughout the airplane. One solution lies in the use of one of the three general types of auxiliary power plant which are as follows:

1. Battery chargers.
2. Battery replacers.
3. Continuous duty (providing a primary source of power).

A further breakdown can be made as to the type of electrical output. In this paper it will be done only insofar as the necessity arises.

Battery Chargers

Without altering the accessory system, except for the addition of a little weight, a battery-charging auxiliary power plant can expand its efficiency materially. One main difficulty thus is overcome; batteries can be recharged readily innumerable times independently of the main engines.

The engine itself must be of simple and rugged construction and as small as possible, consistent with the desired charging rate of the unit. For this type of operation a single-cylinder air-cooled two-cycle engine is a good choice since the number of parts inherently is kept to a minimum. The governor need be no more complicated or accurate than the pneumatic-paddle type used on small engines. A flywheel magneto offers the advantage of having a large part of its

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weight performing useful work as flywheel inertia. Battery ignition, if lighter, can be used since the number of batteries with which the unit operates hardly can be discharged to the point where sufficient voltage for ignition is not available. There is no need for a fuel pump if the unit can be located slightly below the normal fuel level. However, too much fuel head may complicate the carburetor float-valve arrangement. The choice of cooling system depends somewhat upon the requirements of the generator.

A unit of this type has been in use for over a decade in the Navy's flying boats. It consists of a small one-cylinder two-cycle engine driving a combination a-c and d-c generator and has provisions for attaching another accessory. Its long record speaks for itself.

Battery Replacer

The great strides made in the development of generators for main engines resulted in a set back for the continuous-duty type of auxiliary power plant which was being developed to overcome, among other things, low generator output. The resulting increase in electrical loads on airplanes made batteries even less adequate as a source of auxiliary power and led to the development of an auxiliary power plant for replacing batteries.

The battery-replacing type, like the battery charger, is best suited to military aircraft and flying boats, as its main purpose is to provide a more adequate source of auxiliary power where ground-type units are not available. Development of its inherent advantages will make it a common sight in postwar airplanes of all types.

Figure 1 indicates an approximate battery-output curve for a given load as compared with the output curve of an auxiliary power plant. Assuming that the efficiency of the two systems is approximately equal (that is converting of British thermal units in gasoline to kilowatts of auxiliary power) an obvious advantage is apparent.

The output of this type of unit is determined by the largest necessary continuous load required. Although it would be desirable to have sufficient output to handle breakaway surges and to operate several accessories simultaneously, the weight would be objectionable. Figure 2 illustrates roughly the demand curve of an electrical accessory as compared with the output curve of a battery-replacing auxiliary power plant of proper size. Note the shaded area indicating the energy to be supplied by the remaining battery

The engine for the battery-replacing auxiliary power plant is larger than for the battery charger and naturally offers a wider choice as to arrangement. The number of cylinders may vary from two

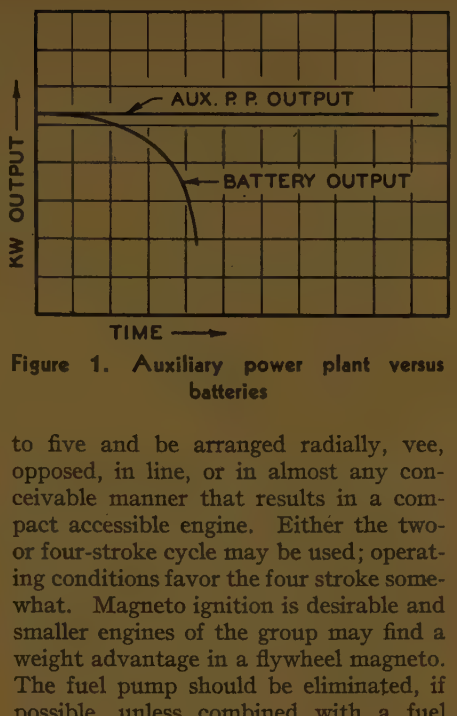


Figure 1. Auxiliary power plant versus batteries

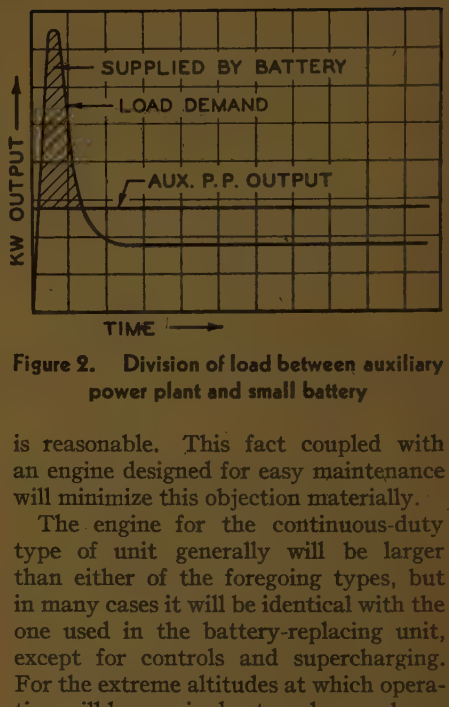


Figure 2. Division of load between auxiliary power plant and small battery

to five and be arranged radially, vee, opposed, in line, or in almost any conceivable manner that results in a compact accessible engine. Either the two- or four-stroke cycle may be used; operating conditions favor the four stroke somewhat. Magneto ignition is desirable and smaller engines of the group may find a weight advantage in a flywheel magneto. The fuel pump should be eliminated, if possible, unless combined with a fuel injector which becomes more practical on larger engines. From the standpoint of first cost and servicing, consideration must be given to the possibility of designing a majority of parts for use in a series of models having varying outputs. This should not be done to the detriment of weight and performance. Controls and other equipment must be determined by the requirements of each installation.

Continuous Duty

The third type of auxiliary power plant, previously mentioned briefly, is one which runs continuously providing a primary source of power. Although certain units of this type have given good service, their development was not advanced sufficiently to compete with improved main-engine-driven generators, and manufacturers turned to the second type mentioned.

Prospects of return of the continuous-duty type are likely when the advantages are considered; some of these are as follows:

1. It rounds out the power system into a complete unit independent of main engines and ground equipment.

2. High-voltage alternators can be used individually or in parallel.

3. Congestion of main-engine accessory sections can be relieved.

Further elaboration on these advantages is unnecessary since they have been discussed many times in various papers. However, one substantial disadvantage does bear mentioning at this time: auxiliary power plants require essentially the same maintenance as main engines do.

Performance records indicate that time between overhauls beyond 1,000 hours is reasonable. This fact coupled with an engine designed for easy maintenance will minimize this objection materially.

The engine for the continuous-duty type of unit generally will be larger than either of the foregoing types, but in many cases it will be identical with the one used in the battery-replacing unit, except for controls and supercharging. For the extreme altitudes at which operation will be required external supercharging has many advantages. Supplementary cooling will be necessary and may be obtained from an air scoop.

As the size of airplanes and, consequently, the demand for auxiliary power increases beyond a certain point, another primary source of auxiliary power may be developed in the form of a steam turbine. Recent trends toward using exhaust gases for deicing as well as for turbosuperchargers might preclude their use as heat for generating steam. This should not hamper the system a great deal since a separately fired boiler would be desirable to provide power independently of main engines.

In order to have auxiliary power plants meet the requirements in the best possible way, close co-operation between manufacturers of airplanes, main engines, auxiliary engines, and accessories (electrical) will be essential.

One of the best ways to save weight on auxiliary engines is to eliminate accessories and structures that are duplicated elsewhere in the airplane. In an effort to relieve the congestion behind main engines some airplanes are equipped with gear boxes. By designing into the gear box an auxiliary engine which would make use of the generator, oil pump, and fuel pump already required, considerable weight could be saved over batteries or conventional auxiliary power-plant installations. The auxiliary engine would drive only the necessary components of the gear box to keep friction at a minimum and would be operated when needed. During flight the auxiliary engine could be kept warm and ready for use by circulating gear-box oil through it.

Continuous-duty auxiliary power plants, like main engines, require supercharging. With a slight increase in the size of the main engine supercharger

The Computation of Magnetic Field Strength of Round Coils of Small Cross Section

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WHEN a solenoid, or cylindrical coil, has no iron core, the magnetic field in the space around it or within it may be computed very precisely. Formulas have been published[1] which take account of the length and thickness of the coil by algebraic terms which have been obtained by an integration over the cross section of the coil. This requires a calculation to be made twice, once for the end of the coil nearer the point for which the magnetic field is being computed, and once for the farther end. When the distance to this point is large compared with the dimensions of the coil section, the final computed result is a small difference of two larger quantities, resulting in a loss of accuracy. In this paper formulas for the magnetic field strength are presented which not only preserve the accuracy of computation, in such cases where they are applicable, but also save a considerable amount of work.

When the length and thickness of the solenoid or coil are so small compared with the radius that they may be neglected, formulas are available which give the field strength directly in terms of elliptic integrals[2] or of a series of zonal harmonics (see equations 12 to 24 of reference 1, and the earlier references given therein). These formulas can be used with actual coils by taking the position and diameter of the coil to be those of the circle at the middle of the coil section.

In this paper, correction formulas are given which can be used along with those of the preceding paragraph, and which will give the effect of the dimensions of the cross section. These formulas are in the form of convergent series, and, as is usual in such cases, their applicability is determined by the rapidity with which

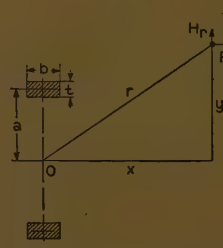


Figure 1. Circular coil

the terms become smaller and smaller, for any given problem. If in any case they are found inapplicable for this reason, recourse may be had to the more general set of formulas indicated by numbers in Figure 5, of reference 1.

A general method by which correction terms for the section of a cylindrical coil may be derived is given in "Electricity and Magnetism" by J. C. Maxwell, volume 2, para. 700. If the coil section, which is a rectangle of length b and radial dimension t, be plotted with the middle point of the section as origin, then the magnetic field due to any small circular wire through the point (u, v) may be called H_{uv}. Expressing this as H_o and a series of its derivatives, and integrating from $-t/2$ to $t/2$ and from $-b/2$ to $b/2$, there results

$$H_{coil} = H_o + \Delta H_o$$

H_o is the magnetic field strength due to the single turn at the center of the cross section, the entire current of the cross section being taken to be concentrated in this turn. H_o may be computed by any of the formulas for single circular turns described in the preceding paragraphs.

$$\Delta H_o = \frac{1}{1!3!2^2}\left(t^2\frac{\partial^2 H_o}{\partial a^2} + b^2\frac{\partial^2 H_o}{\partial x^2}\right) + \frac{1}{1!5!2^4}\times \left(t^4\frac{\partial^4 H_o}{\partial a^4} + b^4\frac{\partial^4 H_o}{\partial x^4}\right) + \frac{1}{3!3!2^4}t^2b^2\frac{\partial^4 H_o}{\partial a^2 \partial x^2} + \ldots \quad (1)$$

A case in which the method of this paper is very applicable is where the point $P(x, y)$ (see Figure 1), at which the value of the magnetic field strength is desired, is at a large distance from the coil.

A formula for the axial component of the magnetic field for this case is equation 20, reference 1, as follows:

$$H_{xo} = \frac{2\pi NI}{10r}\left[\frac{a^2}{r^2}P_2\left(\frac{x}{r}\right) - \frac{3}{2}\frac{a^4}{r^4}P_4\left(\frac{x}{r}\right) + \frac{3\cdot5}{2\cdot4}\frac{a^6}{r^6}P_6\left(\frac{x}{r}\right) - \frac{3\cdot5\cdot7}{2\cdot4\cdot6}\frac{a^8}{r^8}P_8\left(\frac{x}{r}\right) + \ldots\right] \quad (2)$$

By applying equation 1, there is obtained

$$\Delta H_{xo} = \frac{2\pi NI}{10r}\left[C_{(2)}\frac{a^2}{r^2}P_2\left(\frac{x}{r}\right) - C_{(4)}\frac{3}{2}\frac{a^4}{r^4}P_4\left(\frac{x}{r}\right) + C_{(6)}\frac{3\cdot5}{2\cdot4}\frac{a^6}{r^6}P_6\left(\frac{x}{r}\right) - C_{(8)}\frac{3\cdot5\cdot7}{2\cdot4\cdot6}\frac{a^8}{r^8}P_8\left(\frac{x}{r}\right) + \ldots\right] \quad (3)$$

where

$$C_{(2)} = \frac{t^2}{12a^2}$$

$$C_{(4)} = \left(\frac{t^2}{2a^2} - \frac{b^2}{3a^2} + \frac{t^4}{80a^4} - \frac{t^2b^2}{36a^4}\right)$$

$$C_{(6)} = \left(\frac{5}{4}\frac{t^2}{a^2} - \frac{b^2}{a^2} + \frac{3}{16}\frac{t^4}{a^4} - \frac{t^2b^2}{2a^4} + \frac{b^4}{10a^4} + \ldots\right)$$

$$C_{(8)} = \left(\frac{7}{3}\frac{t^2}{a^2} - \frac{2b^2}{a^2} + \frac{7}{8}\frac{t^4}{a^4} - \frac{5}{2}\frac{t^2b^2}{a^4} + \frac{3}{5}\frac{b^4}{a^4} + \ldots\right)$$

Similarly, the radial component at the point $P(x, y)$ is, by equation 21, reference 1,

$$H_{ro} = \frac{\pi NIy}{10r^2}\left[\frac{a^2}{r^2}P_2'\left(\frac{x}{r}\right) - \frac{3}{4}\frac{a^4}{r^4}P_4'\left(\frac{x}{r}\right) + \frac{3\cdot5}{4\cdot6}\frac{a^6}{r^6}P_6'\left(\frac{x}{r}\right) - \frac{3\cdot5\cdot7}{4\cdot6\cdot8}\frac{a^8}{r^8}P_8'\left(\frac{x}{r}\right) + \ldots\right] \quad (4)$$

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enough air could be supplied for the auxiliary power plant more efficiently than from a separate supercharger built into the auxiliary engine. This has been done with good results.

Another source of "cheap" supercharging may be found in the exhaust from cabin supercharging. The auxiliary power plant could be mounted inside or outside the pressurized cabin. However, mounting it inside the cabin would be better from the standpoint of heat dissipation.

Auxiliary-engine accessories such as magnetos, fuel pumps, and carburetors can stand considerable improvement from the weight standpoint. Such equipment on most auxiliary engines is capable of serving engines many times larger.

Although auxiliary power plants necessarily are operating within the range of diminishing returns from the standpoint of weight and efficiency they reopen the avenues of development to designers of electrical equipment. Co-operation along the lines mentioned can result in better and lighter accessory power systems.

The same coefficients are used for each term as in equation 3; thus, by equation 1,

$$\Delta H_{ro}=\frac{\pi NIy}{10r^2}\left[C_{(2)}\frac{a^2}{r^2}P_2'\left(\frac{x}{r}\right)-C_{(4)}\frac{3}{4}\frac{a^4}{r^4}P_4'\left(\frac{x}{r}\right)+C_{(6)}\frac{3\cdot5}{4\cdot6}\frac{a^6}{r^6}P_6'\left(\frac{x}{r}\right)-C_{(8)}\frac{3\cdot5\cdot7}{4\cdot6\cdot8}\frac{a^8}{r^8}P_8'\left(\frac{x}{r}\right)+\ldots\right] \quad (5)$$

In the above formulas, the magnetic field is given at the point (x, y), the center of the round coil being the origin of co-ordinates and the axis of the coil being the axis of x. See Figure 1. The quantity $r^2=x^2+y^2$. The coil has a mean radius of a centimeters. Its cross section is rectangular, the radial thickness being t and the axial length, b. It is evident from the form of equations 3 and 5 that t and b should be small compared with the radius a for these formulas to be applicable. Dimensions are in centimeters. The dots represent multiplication.

$P_n(\mu)$ is a surface zonal harmonic. It may be computed by standard formulas[3] or the required values may be taken from tables.[4] $P_n'(\mu)=\frac{d}{d\mu}P_n(\mu)$.

Since these formulas are to be used when there is no iron or magnetic material near the coil, the flux density at the point $P(x, y)$, in lines per square centimeter, is numerically equal to the value of H given by the formulas.

Example 1. For a numerical example, take $a=10$ centimeters, $t=0$, $b=5$ centimeters, $x=24$ centimeters, and $y=32$ centimeters. See Figure 1. Then:

$r^2=1{,}600$, $r=40$, and $x/r=0.6$

By equation 2,

$$H_{xo}=\frac{2\pi NI}{10^4a}\times\frac{10}{40}(2.500+2.391+0.079-0.007\ldots)=\frac{2\pi NI}{10^4a}\times1.241$$

$$\Delta H_{xo}=\frac{2\pi NI}{10^4a}\times\frac{10}{40}\left(-2.391\times\frac{1}{12}-0.079\times\frac{39}{160}+0.007\times\frac{37}{80}\ldots\right)=\frac{-2\pi NI}{10^4a}\times0.054$$

$$H_x=H_{xo}+\Delta H_{xo}=\frac{2\pi NI}{10^4a}\times1.187$$

An alternative way of computing this result is by two applications of equation 39, reference 1. First, with $t=0$, by equation 39,

$$H_{x_1}=H_{x\infty}-\frac{\pi nI}{10}\left[\frac{a^2}{r_1^2}P_1\left(\frac{x_1}{r_1}\right)-\frac{3}{4}\frac{a^4}{r_1^4}P_3\left(\frac{x_1}{r_1}\right)+\frac{3\cdot5}{4\cdot6}\frac{a^6}{r_1^6}P_5\left(\frac{x_1}{r_1}\right)-\frac{3\cdot5\cdot7}{4\cdot6\cdot8}\frac{a^8}{r_1^8}P_7\left(\frac{x_1}{r_1}\right)+\ldots\right]$$

where

$$H_{x\infty}=\frac{2\pi nI}{10}$$

$$n=\frac{N}{b}=\text{turns per centimeter}$$

$$r_1^2=x_1^2+y^2$$

$$x_1=x-\frac{b}{2}=24-2.5=21.5 \text{ centimeters}$$

$$H_{x_1}=\frac{\pi NI}{10a}\times\frac{a}{b}[2-0.037523-0.001368+0.0000090+0.0000034+0.0000001\ldots]=\frac{\pi NI}{10a}\times\frac{10}{5}\times1.961121$$

For

$$x_2=x+\frac{b}{2}=24+2.5=26.5,$$

$$H_{x_2}=\frac{\pi NI}{10a}\times\frac{10}{5}\times1.962307$$

$$H_x=H_{x_2}-H_{x_1}=\frac{2\pi NI}{10a}(1.962307-1.961121)=\frac{2\pi NI}{10^4a}\times1.186$$

This agrees very well with the result by means of ΔH_o. The method by equation 39 takes about twice as long as equations 2 and 3, for this example. It has the advantage that further terms can be computed indefinitely if desired.

For the same shape of coil as has been taken up, namely, with t and b small compared with the radius a, the magnetic field strength near the center of the coil can be computed by means of equation 1, by applying it to equations 17 and 19 of reference 1.

By equation 17,

$$H_{xo}=\frac{2\pi NI}{10a}\left[1-\frac{3}{2}\frac{r^2}{a^2}P_2\left(\frac{x}{r}\right)+\frac{3\cdot5}{2\cdot4}\frac{r^4}{a^4}P_4\left(\frac{x}{r}\right)-\frac{3\cdot5\cdot7}{2\cdot4\cdot6}\frac{r^6}{a^6}P_6\left(\frac{x}{r}\right)+\ldots\right] \quad (6)$$

By equation 1,

$$\Delta H_{xo}=\frac{2\pi NI}{10a}\left[D_{(0)}-D_{(2)}\frac{3}{2}\frac{r^2}{a^2}P_2\left(\frac{x}{r}\right)+D_{(4)}\frac{3\cdot5}{2\cdot4}\frac{r^4}{a^4}P_4\left(\frac{x}{r}\right)-D_{(6)}\frac{3\cdot5\cdot7}{2\cdot4\cdot6}\frac{r^6}{a^6}\times P_6\left(\frac{x}{r}\right)+\ldots\right] \quad (7)$$

where

$$D_{(0)}=\left(\frac{t^2}{12a^2}-\frac{b^2}{8a^2}+\frac{t^4}{80a^4}-\frac{t^2b^2}{16a^4}+\frac{3}{128}\frac{b^4}{a^4}\ldots\right)$$

$$D_{(2)}=\left(\frac{t^2}{2a^2}-\frac{5}{8}\frac{b^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}-\frac{25}{32}\frac{t^2b^2}{a^4}+\frac{35}{128}\frac{b^4}{a^4}\ldots\right)$$

$$D_{(4)}=\left(\frac{5}{4}\frac{t^2}{a^2}-\frac{35}{24}\frac{b^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}-\frac{245}{72}\frac{t^2b^2}{a^4}+\frac{147}{128}\frac{b^4}{a^4}\ldots\right)$$

$$D_{(6)}=\left(\frac{7}{3}\frac{t^2}{a^2}-\frac{21}{8}\frac{b^2}{a^2}+\frac{21}{8}\frac{t^4}{a^4}-\frac{315}{32}\frac{t^2b^2}{a^4}+\frac{2{,}079}{640}\frac{b^4}{a^4}\ldots\right)$$

For the radial component of the field near the coil center, by equation 19, reference 1,

$$H_{ro}=\frac{\pi NIyr}{10a^3}\left[P_2'\left(\frac{x}{r}\right)-\frac{3}{4}\frac{r^2}{a^2}P_4'\left(\frac{x}{r}\right)+\frac{3\cdot5}{4\cdot6}\frac{r^4}{a^4}P_6'\left(\frac{x}{r}\right)-\ldots\right] \quad (8)$$

and, by equation 1,

$$\Delta H_{ro}=\frac{\pi NIyr}{10a^3}\left[D_{(2)}P_2'\left(\frac{x}{r}\right)-D_{(4)}\frac{3}{4}\frac{r^2}{a^2}P_4'\left(\frac{x}{r}\right)+D_{(6)}\frac{3\cdot5}{4\cdot6}\frac{r^4}{a^4}\times P_6'\left(\frac{x}{r}\right)-\ldots\right] \quad (9)$$

where $r^2=x^2+y^2$ and where the coefficients $D_{(2)}$, $D_{(4)}$, and $D_{(6)}$ are given after equation 7.

The results described in this paper were derived in connection with a thesis at Massachusetts Institute of Technology.

Formulas for the first few surface zonal harmonics are:

$$P_1(\mu)=\mu \qquad P_2(\mu)=\tfrac{1}{2}(3\mu^2-1)$$
$$P_3(\mu)=\tfrac{1}{2}(5\mu^3-3\mu)$$
$$P_4(\mu)=\frac{1}{2\cdot4}(5\cdot7\mu^4-2\cdot3\cdot5\mu^2+1\cdot3)$$
$$P_5(\mu)=\frac{1}{2\cdot4}(7\cdot9\mu^5-2\cdot5\cdot7\mu^3+3\cdot5\mu)$$
$$P_6(\mu)=\frac{1}{2\cdot4\cdot6}(7\cdot9\cdot11\mu^6-3\cdot5\cdot7\cdot9\mu^4+3\cdot3\cdot5\cdot7\mu^2-1\cdot3\cdot5)$$
$$P_8(\mu)=\frac{1}{2\cdot4\cdot6\cdot8}(9\cdot11\cdot13\cdot15\mu^8-4\cdot7\cdot9\cdot11\cdot13\mu^6+6\cdot5\cdot7\cdot9\cdot11\mu^4-4\cdot3\cdot5\cdot7\cdot9\mu^2+1\cdot3\cdot5\cdot7)$$
$$P_1'(\mu)=1 \qquad P_2'(\mu)=3\mu$$
$$P_3'(\mu)=\tfrac{1}{2}(3\cdot5\mu^2-1\cdot3)$$
$$P_4'(\mu)=\tfrac{1}{2}(5\cdot7\mu^3-3\cdot5\mu)$$

and so on, by simple differentiation of the formulas given above.

References

1. Formulas for the Magnetic Field Strength Near a Cylindrical Coil, H. B. Dwight. AIEE Transactions, volume 61, 1942, June section, pages 327–33.

2. Alternating Currents (book, second edition), A. Russell. Cambridge University Press, 1914. Volume 1, page 98, equations 21 and 22.

3. Fourier's Series and Spherical, Cylindrical, and Ellipsoidal Harmonics (book), W. E. Byerly. Ginn and Company, Boston, Mass., 1893. Pages 151 and 198 ($n=1$). See also the last paragraph of this paper.

4. Mathematical Tables (book, first edition third impression), H. B. Dwight. McGraw-Hill Book Company, New York, N. Y., 1941. Page 188 and following.

Aircraft-Engine Torque Instruments

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Synopsis: Flight-operations experience has shown that significant improvements in fuel economy may be obtained through the use of aircraft-engine torque instruments as regular flight instruments. This paper describes the better-known types of torque instruments and introduces a new type that has recently come through its development stage.

THE performance of airplane engines in flight is evaluated ordinarily by means of engine speed, manifold pressures, air temperature, airplane speed, and flight altitude in terms based upon factory-calibrated engines. Corrections for fuel variations, atmospheric humidity, and actual engine condition are usually indeterminate and cannot be applied, so that substantial variations from the "chart" engine output frequently are encountered. Torque measuring instruments have been used when available for test flying of new aircraft designs and to a limited extent as regular flight instruments to reduce fuel consumption and extend the useful radius of flight operations. The value of a torque instrument on the propeller shaft of an airplane engine lies in a balance between the advantage of knowing true engine-power delivery and the disadvantage of additional weight, service difficulties, and increased equipment cost. For very long-range operations of large military aircraft, engine torque instruments or some accurate type of power-measuring devices are a necessity and a military requirement. As ranges are shortened and as airplane size decreases, excessive torque-instrument weight and cost increases can quickly outweigh their advantages.

General Types of Torque Instruments

There are two general types of torque instruments which have been applied to aircraft engines to determine engine output; these are the hydraulic and the electrical types. The hydraulic torque instrument[1,2] is the better known of the two and has been in use for a number of years; it usually measures the torque reaction on a stationary member of the propeller-shaft reduction-gear train by means of an hydraulic piston and a pressure-regulated oil supply. The oil pressure is controlled by means of slight piston movements to balance exactly the torque reaction of the selected gear, usually the stationary sun gear of a planetary gear set, Figure 1, and the oil pressure which is proportional to the torque is measured by a conventional pressure gauge. When double-reduction gears are required, or when operation at an extremely low temperature is necessary, and when excessive torsional vibrations are present, the hydraulic system becomes difficult to handle. Electric torque instruments fall into three general classifications, the strain-resistive, strain-capacitive, and strain-magnetic.

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Electrical-Resistance Strain Gauges

The strain-resistive type may measure either the twist in the propeller shaft by means of strain-sensitive resistance elements mounted directly on the shaft, or it may measure the deflections of a stationary reaction member, somewhat similar to the conventional hydraulic torque instrument in application except that the actual deflection of a spring is measured rather than the balancing oil pressure. If the strain-sensitive resistance elements are attached to a stationary reaction member, direct electric connections can be made between the resistance units and the indicating instrument; but if the resistance units are mounted on a rotating shaft so as to be sensitive to the actual twist in a specified gauge length of shaft, electric connections usually must be made through slip rings on the shaft with brushes riding on the slip rings, Figure 2. Attempts have been made to use carbon-pile resistance elements in such torque instrument or strain-gauge applications because of the high energy levels which can be tolerated in carbon piles. This should make possible direct indication of torque on conventional ammeters or voltmeters without using amplifiers; however, the strain-resistance characteristics of carbon piles have not been sufficiently stable and subject to manufacturing control within required accuracy limits to permit their use. Wire-resistance strain gauges[3] have the required accuracy, but the resistance changes for strains corresponding to maximum allowable stresses in both the engine parts and the wire-resistance elements seldom exceed one per cent of the initial resistance. Therefore, use of a sensitive potentiometer and balancing bridge is required with wire-resistance strain gauges, or high-gain electric amplifiers must be used with direct-indicating instruments. The ease of application of the small resistance-wire strain gauge and its general reliability makes it quite valuable for laboratory work, but its low sensitivity appears to rule it out as an aircraft flight instrument. A further disadvantage is the relatively large temperature coefficient of resistance of most of the wire-resistance strain gauges; normal temperature excursions in aircraft engines result in temperature-resistance changes of greater magnitude than the strain-resistance changes under measurement; these temperature effects can be eliminated, however, by careful adjustment of compensators.

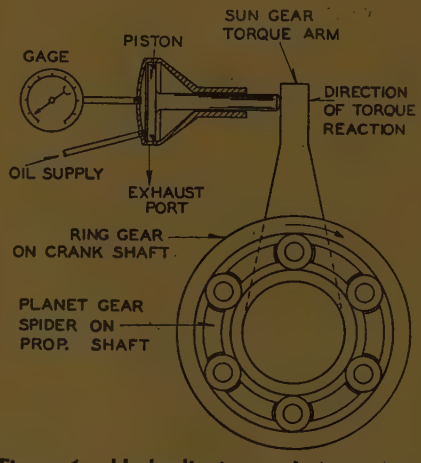


Figure 1. Hydraulic torque instrument applied to sun gear of planetary transmission

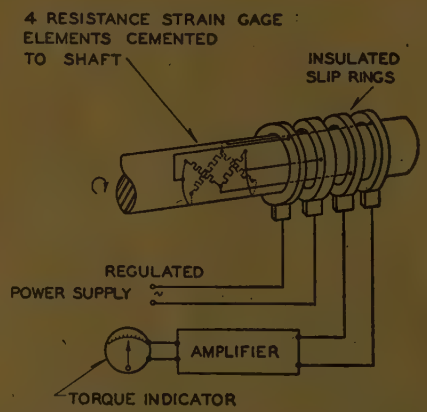


Figure 2. Torque instrument using resistance-wire strain gauges attached to shaft

Electrical-Capacitance Strain Gauges

Strain-capacitive elements are used in much the same manner as strain-resistive elements, and may be mounted either on rotating shafts with slip-ring connections or on torque-responsive stationary members of the engine. Here an air-dielectric capacitor is formed of two groups of plates or conducting surfaces insulated from each other and so positioned that, when the assembly is subjected to strain, either dielectric spacing between plates of opposite polarity changes or opposed plate areas

alter in proportion to the strain, with a resultant change in the capacitance of the capacitor. The per cent change in capacitance which can be obtained for permissible strains in engine parts can be large, and for that reason much effort has been expended in attempts to utilize strain-capacitive elements in strain gauges and torque instruments. However, space and weight limitations usually restrict the capacitance of the strain-sensitive element to a few micromicrofarads; and therefore, even with a-c power supplies of several hundred kilocycles frequency, energy levels in the sensitive circuit elements are so low as to require amplifiers ahead of the torque indicating instrument, with the usually attendant difficulties of maintaining calibration. Capacitance-sensitive strain elements must also be hermetically sealed to exclude oil, moisture, and dirt; and the uncertain stray capacitances of electric connections and mechanical supports are also major difficulties.

Electromagnetic Strain Gauges

Strain-magnetic elements lend themselves to the measurement of small displacements much more readily than do any of the aforementioned strain-sensitive devices when it is desirable to connect direct-indicating instruments to the strain-sensitive circuit without interposing amplifiers. The conventional magnetic strain gauges operates at sufficiently high energy levels that its output can be utilized to drive an indicating instrument directly; and in such manner it is commonly used as the sensitive element in many precision gauges or comparators as well as for aircraft-engine testing.[4]

It is usually small and operates on conventional a-c supply circuits, 60 cycles in the case of ground equipment and 400 cycles for air-borne equipment, and can take any one of several different forms; the one in Figure 3 with its connection diagram shown in Figure 4 is simple and commonly used. It consists of two iron-core inductance coils with variable-length air gaps in their magnetic paths; moving the armature A of Figure 4 to the left will decrease the air gap of coil Z_1, increasing its inductance, and at the same time will increase the air gap of coil Z_2 with a corresponding decrease of its inductance. Since Z_1 and Z_2 are both elements of a four-arm bridge, the output of the bridge indicated by the instrument M will be almost exactly proportional to the physical displacement of the armature A from its center position, within the useful limits of the strain gauge. The other two bridge arms, Z_3 and Z_4, are usually made part of a center-tapped autotransformer, which decreases the bridge impedance and thus increases the maximum signal energy available.

Magnetic strain gauges can be mounted in the take-off head of the conventional hydraulic torque instrument, replacing the hydraulic piston and pressure gauge. When this is done, a spring element, usually a short section of steel tubing, resists the reaction of the sun-gear torque arm and the strain gauge measures the deflections of the steel tube, Figure 5. They also have been mounted directly upon the propeller shaft of an engine or reduction-gear assembly, Figure 6, with electric connections made through slip rings on the shaft.

The Magnetic-Coupled Torquemeter

For shaft mounting, the slip-ring design is likely to be difficult from the standpoints of service trouble and contact resistance. On the other hand, there are numerous aircraft-engine applications for torque instruments where it is desirable if not imperative to measure the twist in a shaft when the engine output is to be known with any degree of accuracy. The Magnetic-Coupled Torquemeter therefore was designed to eliminate slip rings and still retain a shaft-mounted strain gauge. A schematic illustration appears in Figure 7, and a picture of a typical assembly is reproduced in Figure 8. If we refer to Figure 7, the shaft is provided with three flanges on which are mounted three toothed rings of magnetic material, but separated from contact with the steel shaft by means of nonmagnetic spacers made of a metal such as bronze or brass. Overlapping teeth or projections from each of the three magnetic rings form two sets of air gaps, one set between outer ring 1 and the middle ring A, and one set between outer ring 2 and the middle ring A. When the shaft is loaded or twisted in a clockwise direction, the air gaps between teeth on rings 1 and A are shortened and the air gaps between teeth on rings 2 and A are lengthened; this is in effect the same as moving the armature A of Figure 4 to the left.

Magnetic flux flows across the two sets

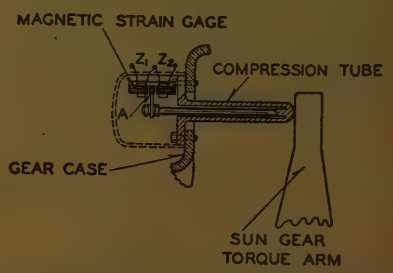


Figure 5. Magnetic strain gauge applied to sun gear of planetary transmission

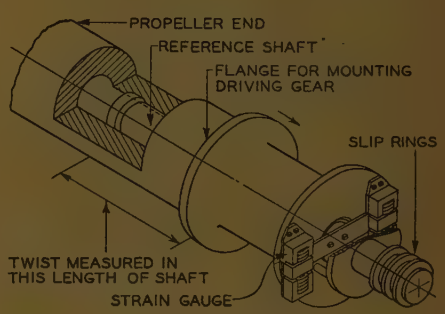


Figure 6. Magnetic strain gauges measuring the twist of a propeller shaft

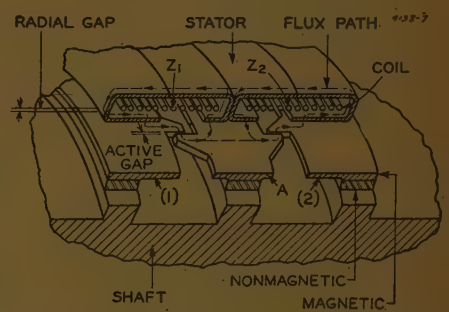


Figure 7. Schematic illustration of Magnetic-Coupled Torquemeter



Figure 3. Magnetic strain gauge

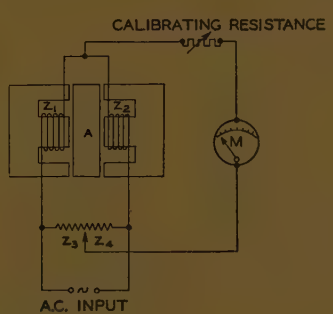


Figure 4. Simplified magnetic-strain-gauge circuit



Figure 8. Small Magnetic-Coupled Torquemeter for test-floor use



Figure 9. Magnetic-Coupled Torquemeter designed for propeller shaft of Allison V-1710-E engine

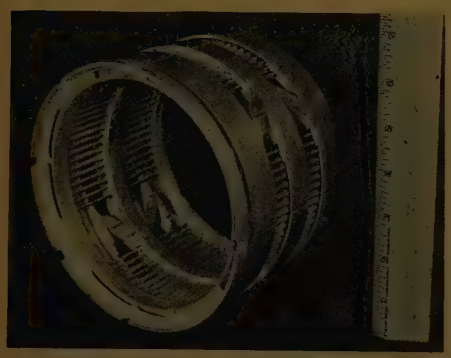


Figure 10. Magnetic-Coupled Torquemeter rotor with four tooth gaps per side

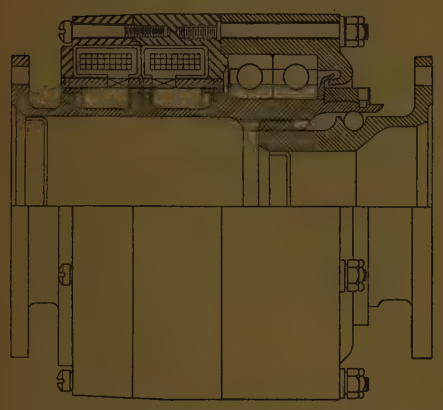


Figure 11. Magnetic-Coupled Torquemeter designed for the engine extension shaft of an Allison engine

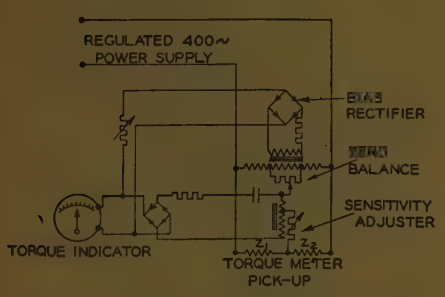


Figure 12. Electric circuit for a magnetic strain gauge or Magnetic-Coupled Torquemeter using a regulated power supply

of air gaps, induced by the two stationary coils Z_1 and Z_2, also corresponding to Z_1 and Z_2 of Figure 4. Coils Z_1 and Z_2 are shown completely encircling the shaft assembly and the magnetic return paths are through their encasing shells of magnetic material and across the radial air gaps between the stationary-coil assembly and the rotating-shaft assembly. These air gaps have relatively large effective areas and short lengths so that their magnetic permeances are quite large compared with the permeances of the active air gaps between the toothed rings; consequently, the insertion of the radial air gaps in the magnetic circuit has but small effect upon the characteristics of the strain gauge. Normal eccentric movements of the rotating shaft in its bearings relative to the stator cause second-order changes in permeance of the radial air gaps and finally result in only third-order changes in torque-instrument calibration.

When strain-gauge elements are mounted on a rotating shaft, precautions must be taken to avoid errors due to bending moments and compression or tension loads on the shaft so that only pure torque is measured. This requires that a balanced magnetic circuit be used with both Z_1 and Z_2 responsive to shaft torque loads, and it also requires that the sensitive air gaps be distributed uniformly around the circumference of the shaft. Slip-ring installations of two conventional magnetic strain gauges diametrically opposite each other on a shaft and with their coil circuits either connected in series or in parallel have shown a satisfactory lack of response to bending loads on the shaft where moderate loads are imposed. Some aircraft-engine propeller shafts are subjected to bending moments from propeller gyroscopic couples in spins that may result in maximum fiber stresses of the order of 100,000 pounds per square inch where normal stress in torsion is of the order of 8,000 to 16,000 pounds per square inch.

Well-distributed sets of sensitive air gaps are desirable in such circumstances, and Figure 9 shows a rotor for a magnetic-coupled torque instrument with a large number of small individual air gaps in each set around the shaft. This particular shaft element is intended to be mounted between two existing flanges on an engine propeller shaft, and therefore, in order to divide circumferential strain due to twist in the shaft length between flanges equally between the two sets of air gaps, three rotor rings are mounted on a torsionally flexible cage, the ends of which are permanently fastened to the two flanges. The stator carrying the two coils and their magnetic shells is also shown in the same illustration, and is bolted to the inner-thrust-bearing retainer ring inside of the propeller reduction-gear housing.

The large number of small air gaps shown in the rotor of Figure 9 is very effective in averaging out bending and thrust loads on the propeller shaft, but does lead to excessive magnetic leakage around the teeth and also to increased manufacturing difficulties since normal air-gap spacing is of the order of 0.004 to 0.010 inch, depending upon shaft stiffness characteristics. Another rotor for the same application is shown in Figure 10 in which only four air gaps each of relatively large area are used per sensitive gap set; there is a considerable increase in apparent sensitivity of this torque instrument over the one shown in Figure 9, due to the better magnetic design and the large reduction in leakage flux around the rotor. Flux leakage at 400 cycles per second to the steel shaft is effectively reduced to a negligible quantity if the nonmagnetic spacer rings between the toothed rings and the shaft are made of high-conductivity materials such as brass, bronze, or copper.

Figure 11 shows another magnetic-coupled torque instrument intended for use on the extension shaft between the engine and a separate propeller reduction-gear box. The three toothed magnetic rings are mounted on three shaft flanges instead of on a flexible cage, and the short shaft section carrying the rings is inserted in the extension-shaft drive with flanged or splined couplings. A better mechanical job is obtained when it is possible to mount the toothed rings directly upon individual flanges integral with the shaft rather than upon the surface of a flexible cage or sleeve.

Temperature Effects in Magnetic Strain Gauges

The effects of ambient-temperature changes upon magnetic strain gauges can be divided into those originating in the instrument circuit and those originating in the shaft pickup unit. Temperature effects in the instrument circuit are readily corrected by conventional means and attention to good design practice. The temperature effects in the shaft element are more difficult to handle ordinarily, particularly since temperatures may range from −50 degrees centigrade to hot engine oil temperatures. The changes in modulus of elasticity of the shaft material are usually small over the normal temperature ranges, but serious unbalances of the impedance bridge with large shifts in the zero adjustment of the torque instrument may result from large ambient-temperature changes around the shaft pickup unit; this may be particularly noticeable when large temperature differentials appear from one end of the shaft unit to the other. These calibration changes can be held to small limits if sufficient care is used in design to prevent large hysteresis and eddy-current losses in the magnetic parts of the shaft unit. High power factors in the impedance coils of the strain-gauge bridge usually are the result of abnormal magnetic losses due to saturation effects in parts of the circuit;

and such losses are usually highly temperature-sensitive, so that torque-instrument accuracy suffers from temperature changes. By exercising care in design and application, these temperature effects can be held within acceptable limits; one of the early Magnetic-Coupled Torquemeters that showed excessive temperature sensitivity was corrected by adding a small resistor with a high temperature coefficient of resistivity in series in one of the impedance coil arms of the bridge. A nickel-wire resistor was used and fastened to the shaft pickup stator housing.

Magnetic-Strain-Gauge Instrument Circuits

A number of precautions must be observed in applying magnetic strain gauges or Magnetic-Coupled Torquemeters to aircraft instrumentation problems. The a-c power supplies ordinarily available on airplanes may be subject to rather wide variations in voltage and frequency; and, while close frequency regulation is not essential, the calibration of the torque instruments is proportional to the supply voltage when a simple circuit such as shown in Figure 4 is used. Therefore a voltage-regulated power supply is essential to accurate calibration under these circumstances. Since the electrical load is small and fixed, the voltage-regulator problem is not particularly difficult. The power requirements of a magnetic strain gauge at 400 cycles is about five voltamperes. The Magnetic-Coupled Torquemeter, due to greater iron loss and flux leakage requires 30 to 50 volt-amperes. Ordinary frequency variations are compensated for, when compensation is required, by the use of a small capacitor in the instrument circuit. On airplanes where the only available electric power is direct current, it is possible to use a small inverter to obtain the necessary 400 cycle power. Various regulators, such as the carbon-pile, saturated-reactor, and centrifugal speed-governing type are available. By designing a unit specially for the job, it is possible to reduce the weight of the inverter, complete with regulator, to about two pounds per torque instrument on a two-engine airplane.

The indicating instrument is usually a rectifier-type milliammeter, and the complete bridge output circuit must include the proper external resistances to insure that the instrument circuit will provide accurate voltage measurements of the strain-gauge bridge output over wide ranges of ambient temperature, input voltage, and frequency. Such a circuit is shown in Figure 12, which is Figure 4 with the added refinements of a voltage regulator, frequency-compensating capacitor, sensitivity-adjusting rheostat, zero-balance potentiometer, and a small bias rectifier. The bias rectifier is added, because a true null balance of the bridge is never obtainable because of the presence of phase unbalance and harmonics in the supply voltage. Therefore, without a biasing rectifier or an off-scale zero spring adjustment, the indicator pointer could never be brought to zero.

The use of a d-c ratio instrument instead of a conventional milliammeter eliminates the necessity for the voltage regulator, and since frequency variations are relatively unimportant in magnetic-strain-gauge instrument errors, its use has many attractive features. A d-c ratio instrument consists of a permanent-magnet instrument movement with two coils set at a wide angle to each other on the shaft of the moving element which carries the pointer. Connections are made to the moving coils through soft gold conducting spirals instead of the conventional springs, and the resultant spring restraint is negligible under normal conditions. When each coil is made to carry current, the position which the pointer will assume is determined by the resultant magnetic field of the two coils, and the instrument scale is marked as the ratio of current flowing in the "forward" coil to that flowing in the "reverse" coil. The torque-instrument application of a d-c ratio instrument is shown in Figure 13, and it should be noted that the coils are energized through instrument-type rectifiers and also that a biasing voltage is required for the forward coil for zero-adjustment purposes. Unfortunately, the scale length of present d-c ratio instruments is limited to that of a 90 degree instrument movement, and the scale is nonlinear. A comparison of the short-scale ratio instrument with a 270-degree-scale-movement aircraft-type milliammeter such as is used in the circuit of Figure 12 is shown in Figure 14.

A greatly simplified torque instrument utilizing the magnetic strain gauge working directly into an a-c ratio instrument is shown in the connection diagram of Figure 15, in which voltage regulators, rectifiers, and all biasing circuits are eliminated. The stationary coil of the instrument is energized directly from the power-supply source, and the moving coil is connected to the bridge output. Current is conducted to the moving coil through soft gold spirals with a minimum of spring restraint, and the moving element comes to rest when the inphase component of the voltage induced in the coil is equal to and opposes the bridge output voltage. Since the power factor of the stationary coil is usually quite low, the out-of-phase voltages induced in the moving coil are small and have a negligible effect upon accuracy. Physically the a-c ratio-instrument movement resembles the conventional frequency-instrument movement. Close coupling between the moving coil and the stationary coil is desirable in order to reduce power requirements, and a laminated silicon steel or Hypernik magnetic structure is used. Long-scale or 270-degree a-c instrument movements have not yet been developed for aircraft use, and the a-c ratio-instrument airplane-engine torque instruments probably will have to wait until such movements are available in production. Figure 16 shows an experimental long-scale a-c ratio-instrument movement for aircraft use compared with a modified wattmeter move-

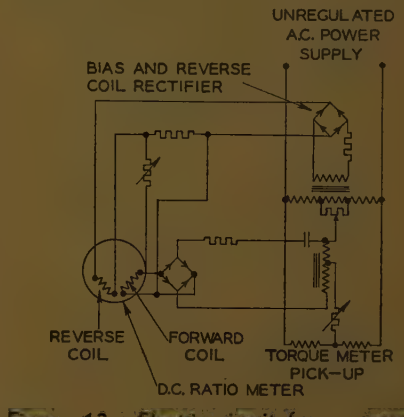


Figure 13. Electric circuit for a magnetic strain gauge or Magnetic-Coupled Torquemeter using an unregulated power supply and a d-c ratio instrument



Figure 14. Aircraft-type d-c milliammeter (left) and d-c ratio instrument (right)

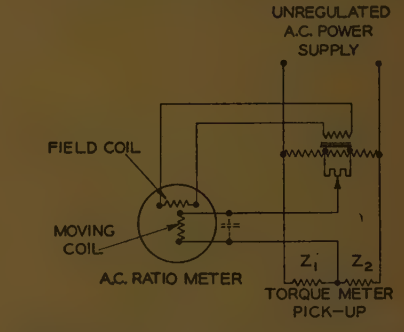


Figure 15. Electric circuit for a magnetic strain gauge using an a-c ratio instrument

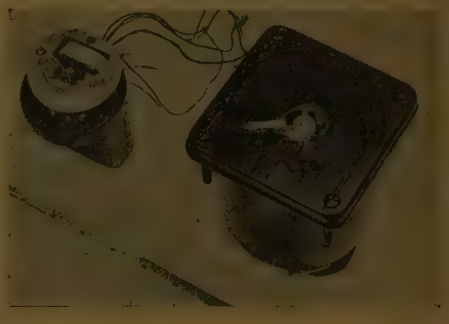


Figure 16. A-c ratio instruments

Experimental movement at left, modified wattmeter at right

ment originally designed for marine use and therefore somewhat large and heavy for aircraft applications; both instrument movements operate successfully as a-c ratio instruments.

Other Shaft-Mounted Types Without Slip Rings

Several experimenters have made attempts to utilize various coupling schemes to permit the use of strain-resistive or strain-capacitive gauges mounted directly upon the rotating shaft without using slip rings or electric connections. Strain-sensitive capacitors have been mounted directly on the torque loaded shaft with their terminals connected to the secondary coils of transformers, these coils completely encircling the shaft and rotating with the shaft; the primary coils of the transformers are stationary and also encircle the shaft assemblies and the rotating coils. Mutual inductance between the primary and secondary coils of the rotating transformer thus formed constitutes the link between the stationary instrument circuit elements and the strain-sensitive capacitor on the loaded shaft.

Coils also have been placed close to or encircling shafts to cause magnetic flux to flow in the surface of the shaft itself or through special surface coatings on the shaft, so that, when the shaft is twisted under torque loads and the magnetic permeability and electrical resistivity of the shaft change, such changes are reflected as changes in impedance of the flux-inducing coil or coils.[5] This magnetic skin-effect method of torque measurement is sensitive to temperature, shaft bending, mechanical and magnetic hysteresis, and thrust loads.

Relatively high-frequency (kilocycles) power supplies are required for the aforementioned two schemes, and amplifiers are required between the shaft unit and the indicating instrument. The magnetic skin-effect or magnetostriction torque-instrument shaft element approaches the ultimate in design simplicity; and if means of eliminating temperature errors and hysteresis effects can be found, the simplicity of the shaft element may offset the disadvantages and difficulties of high-frequency power supplies and amplifiers.

References

1. Airline Power Control With a Torquemeter, W. G. Lundquist. Society of Automotive Engineers *Journal*, volume 44, June 1939, pages 271–6.

2. Aircraft-Engine Reduction Gears and Torque Meters, Roland Chilton. Society of Automotive Engineers *Transactions*, volume 48, February 1941, pages 66–71.

3. Characteristics and Aircraft Applications of Wire-Resistance Strain Gages, A. V. deForest. *Instruments*, volume 15, April 1942, page 112.

4. Design and Applications of a Magnetic Strain Gage, B. F. Langer. *Proceedings* of Society for Experimental Stress Analysis, volume I, number 2, December 1, 1943.

5. French patent 831,342.

High-Voltage Compressed-Gas Power Transformers

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In a previous article,[1] the author pointed out the many advantages of high-voltage power transmission in compressed gas. Compressed CO_2 at the moderate pressure of 40 atmospheres (absolute) has a dielectric strength about 20 times as high as that of air at normal atmospheric pressure, and compressed SO_2 at 40 atmospheres about 30 times as high, so that the line conductors need be separated by only a few inches instead of the customary many feet and may, therefore, be enclosed in a reasonably small pipe line. This system offers opportunities for many important cumulative economies in the terminal equipment also, and this paper is devoted to their discussion. It is shown that the use of compressed gas, instead of oil, as the insulating and cooling fluid makes possible power transformers that are only about one fourth as large as the present ones for the same duty, are for all intents and purposes nonaging, and are highly resistant to damage from internal arc-overs, besides many other advantages.

While such transformers have not as yet been built, and further laboratory research is desirable on certain phases of the system, yet, based on the great economies shown by several exploratory designs listed in Table II, the author expects that the system will be adopted in the near future to transmit large amounts of power over great distances by transmission lines of very much higher voltages than heretofore and at frequencies as high as 600–1,200 cycles per second.

Important Facts Underlying the System

The more important facts from which the various advantages of the proposed transformer system arise are as follows:

1. The dielectric strength of some compressed gases is greater than that of oil.

2. Gases are not subject to cumulative breakdown to which solid insulation—the backbone of the present-day high-voltage transformers—is subject.

3. Gases have no dielectric loss and can, therefore, be operated at 600–1,000 cycles per second without any harm, making possible corresponding reductions in the sizes of the cores and also of the coils.

4. Unlike oil and the conventional solid insulations used in transformers, gases do not age and are not injured in any way by moderately high temperatures, and therefore the cores of compressed-gas transformers can be operated a couple of hundred degrees centigrade hotter than the windings and the windings a hundred degrees hotter than now to transform much larger kilovolt-amperes per pound than heretofore.

5. For heat transfer, compressed gases are many times more efficient than atmospheric air used in the conventional shell-type air-blast transformers.

These and additional facts and considerations are discussed below in greater detail.

Compressed Gases Versus Oil

Table I shows the leading data on the 60-cycle dielectric strength of several gases at various pressures and that of oil. It will be seen in the table that CO_2 at 40 atmospheres absolute has a 60-cycle strength of 1,650 volts per mil as against 750 volts per mil for oil—a ratio of 2 to 1 in favor of the compressed gas. For transient voltages of steep rise and short duration, such as the 1.5x40 standard impulse wave of the Institute, oil is credited with a strength of twice its 60-cycle value, whereas the strength of the gases is *assumed* to be unchanged. Compressed SO_2 at 40 atmospheres (2,400 volts per mil) is approximately 50 per cent higher than that of either nitrogen or CO_2. No data are now available on the impulse-strength ratios of compressed gases circulated at a high velocity. These may be higher than unity. Laboratory investigation of them, in both uniform and nonuniform fields, is very desirable.

Cumulative Failure of Solid Insulation Under Impulse Voltages

As impulse voltages are of rare occurrence and of very short duration, if a gas does not break down under the sudden stress, it probably will recover from the

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The author is indebted to Glenn B. Warren for suggesting the investigation of underground high-voltage transmission in compressed-gas-filled welded steel pipes, to H. H. Skilling for encouragement derived from his article (reference 3), to the many engineers and scientists mentioned in reference 1, and to the author's own associates.

This paper is speculative and looks toward the future; consequently, it contains many statements of opinion for which there are no supporting data or facts.

shock with its strength unimpaired. But with solid insulations a small permanent harm may be done without apparent failure, and this process will be cumulative. The overvoltage failure of a conventional oil-and-solid-insulated transformer will not take place necessarily at the first heavy surge occasioned by lightning, but after a number of such surges have produced minute but progressive and cumulative impairments. Then, a less severe surge may cause an outage.

Aging of Solid Insulation

It is well-known that the customary fibrous insulations used in oil-filled transformers age physically and become brittle at a rate twice as fast for every eight-degree-centigrade increase in temperature. Every substantial overload thus represents a substantial shortening of the useful life of the transformer. Such an effect does not exist in compressed-gas transformers, and therefore short-time overloads may be handled without any residual harm. Such transformers will be practically nonaging.

Problem of Nonuniform Fields in Gases and Solid Insulations

Uniform distribution of voltage stresses is, of course, desirable no matter what the insulation may be, but in transformers the compressed gases enjoy a particular advantage over solid insulations. In solid insulations overstress implies cumulative damage, and, no matter how small the damage may be per day, cumulation of such damage implies eventual failure. For this reason, in conventional apparatus, the bulk of the solid insulation is operated at a very low and uneconomical stress. A good example of this is the slot-lining insulations in generators and motors. The slot lining of a high-voltage stator winding may prove in the laboratory a strength of several thousand volts per mil in a uniform field and yet, based on experience, be operated at only 30–60 volts per mil on the average.

Because of the greater electronic mobilities in gases, it is wise to proceed at first with generous margins of safety in compressed-gas apparatus, until a sufficient amount of service experience is gained, but such margins of safety are more economically practicable with compressed gases than with oil and solid insulation.

Value of High Frequencies

One of the major economy factors in this system is the use of 600–1,200-cycle frequencies made possible by the replacement of oil and solid insulation by compressed gas. If compressed gas is used in combination with these higher frequencies, sizes drop to about a fourth of their present values.

The voltage produced by a given flux varies directly with the frequency. At tenfold frequency the voltage, and hence the kilovolt-amperes, is ten times as large for the same flux, or the flux required for the same kilovolt-amperes is only one tenth as large. Since, however, at constant flux density, core loss increases with frequency, the same high flux densities may not be used at tenfold frequency as at normal, and the full tenfold benefit may not be secured; yet, by virtue of the permissible high core temperatures and by the use of seven-mil sheet steel it is possible to use sufficiently high flux densities to afford the great economies indicated.

Table I. Dielectric Strength of Four Gases and Commercial Transformer Oil

Tested at 60 Cycles Between Two Spherical Electrodes of 2,500-Mils Diameter With a 100-Mil Gap

Gas Pressure in Atmospheres Absolute	Volts Per Mil (Crest)				
	Air	Nitrogen	CO_2	SO_2	Oil
1	85	95	75	200	750
20	1,300	1,000	1,000	2,000	
40	1,850	1,450	1,650	2,400	
60	2,100	1,550	2,250		

Departure from 60 cycles at this late date may appear to some as impractical, but the best economic course is to utilize 600 cycles (or more) for the major high-voltage transmission system and network of any single enormous territory, and employ frequency changers or rectifiers where the service demands a different frequency or direct current.

These higher frequencies reduce the size not only of the big terminal equipments but also of all power transformers used in the industry. Rectifier installations for electrometallurgical and electrochemical processes, as for the production of aluminum and magnesium, include already several millions of kilovolt-amperes in step-down power transformers. Rectifier applications (all requiring special step-down transformers) for railways, steel mills, mines, and shipyards are rapidly growing. An increase of 10 to 1 in frequency, with compressed-gas insulation, will reduce the size and cost of these transformers to about one seventh.

These higher frequencies are particularly advantageous in welding as to speed, quality, safety, and various other cumulative economies. The a-c arc is more stable and produces a better weld at the higher frequencies, and therefore greater speeds and lower voltages may be used. The combination of lower voltages and higher skin effect will greatly reduce shock hazards to the operator from accidental contact with live electrodes. Careful tests to confirm these conclusions ought to be undertaken as soon as possible.

Resistance-welding and flash-welding systems also will benefit as to speed, quality, and greatly reduced cost of equipment, from a tenfold increase in frequency.

Effect of High Core Temperatures on Efficiency and Economy

As far back as 1911 and 1912 MacLaren published his researches demonstrating substantial reductions in core loss at increasing core temperatures.[2] As core temperatures heretofore have been limited to about 90 degrees centigrade to avoid the sludging of oil, now, with the replacement of oil by compressed gases, it becomes possible to operate the transformer cores at temperatures as high as 300–325 degrees centigrade, so as to realize corresponding reductions in core loss and secure great economies in core material by operating the cores at higher frequencies and flux densities than now possible.

These high temperatures are particularly advantageous in the case of those compressed gases (as SO_2) which tend to liquefy at the lower temperatures.

Thermal Screening of Windings

In a compressed-gas design the windings also will be operated at higher temperatures (though not so high as the core temperatures), so as to enable them to carry greater loads per pound. Higher winding temperatures naturally tend to raise the I^2R, but in this system a higher iron-to-copper ratio would be maintained using relatively more flux and fewer turns, and thus the increase in the I^2R due to higher temperature will be small and may be offset by operating the core at a still higher temperature.

With core temperatures of the order of 300 degrees centigrade the winding temperatures should be 100 degrees less, in which case provision must be made for the thermal screening of the windings from the core. Excellent high-temperature materials are now plentifully available for turn and layer insulation and for spacers and strengthening supports, and for screening the windings from the core. Mycalex, asbestos, lava compositions, or ceramic materials in suitable thicknesses easily will withstand a temperature drop of 100 degrees centigrade between core and windings.

Heat-Transfer Procedure

The stream of circulating gas will be heated by the transformer losses as it ascends from the bottom to the top of the transformer. Above the transformer, or in the upper portions of the tank, condensation may be effected by providing a

region of lower temperature or through an external heat exchanger.

The circulation speed of the gas will be high—500–1,000 feet per minute, controlled by a fan or blower.

As the core will operate around 300 degrees centigrade, and there will be a drop of about 100 degrees centigrade through the thermal barrier, the ascending gas will flow over barrier surfaces at a temperature of about 200 degrees centigrade. By virtue of the high rate of flow of the gas, no portion of it will acquire a temperature higher than 150 degrees centigrade.

The hottest parts of the windings may attain a temperature in excess of 200 degrees centigrade, the surface drop probably exceeding 75 degrees centigrade, and the average copper temperature by resistance being of the order of 170 degrees centigrade.

These hypothetical temperature values are chosen as well suited to the use of SO_2 at 50 atmospheres.

As a comparison between the heat-transfer capacity of compressed gas and air at atmospheric pressure, it may be pointed out that SO_2 at 50 atmospheres is about 71 times as efficient as atmospheric air for equal volume. This is based on a specific-gravity ratio of 110:1 and specific-heat ratio of 0.154:0.237, excluding considerations of viscosity and contact resistance. Thus, instead of the 130 cubic feet of air per minute per kilovolt-ampere for 55 degrees centigrade rise allowed in air-blast-transformer practice, only 1.8 cubic feet of compressed SO_2 at 50 atmospheres would be needed.

Supporting of Transformer Tanks

The forces exerted on the tanks by the compressed gas can be taken care of by the combination of:

(*a*). Providing a tank-wall thickness of $^5/_8$ inch.

(*b*). Providing internal braces in the form of tie rods of a nonmagnetic alloy.

(*c*). Locating the tank in an underground pit and filling in gravel and cement between the tank walls and the pit walls to provide support for the tank walls.

Comments on Sulphur Dioxide

If absolutely free from all trace of moisture, SO_2 gas is quite noncorrosive to some metals up to rather high temperatures. It costs only six cents per pound, whereas many otherwise attractive insulating gases are rather expensive. In a metal-sealed transformer tank complete absence of moisture may be maintained. Although more tests are desirable on the effect of temperature and pressure on the corrosive properties of this gas, rough tests indicate that a pressure of 40 atmospheres is entirely safe. At this pressure its dielectric strength is 2,400 volts per mil and the condensation temperature 120 degrees centigrade. At 50 atmospheres its dielectric strength is 2,500 volts per mil and condensation temperature 130 degrees centigrade. Doubtless reliable surface treatments (such as enameling) could be developed to render the metal surfaces immune from attack at much higher temperatures.

In this, as well as in all other gases, condensation of the gas on those portions of the system which need insulation must be prevented by keeping those parts hotter than the condensing temperature of the compressed gas.

Exploratory Designs

Five designs are exhibited in Table II in just enough detail to disclose clearly the *trends* and illustrate the *potentialities* of compressed-gas transformers. Three of the designs (*A*, *B*, and *C* for 80 kv) are for 20,000 kva each, one at 60 cycles, one at 600, and the third at 1,200. Design *D* is for 50,000 kva at 1,200 cycles per second, and *E* for 100,000 kva at the same frequency.

Design *A* (60 cycles per second) has a per kilovolt-ampere weight of 4.2 pounds, which is approximately half that of a conventional oil design, whereas designs *B* and *C* have weights of only 1.05 and 0.82 pounds per kilovolt-ampere, respectively. The full-load efficiencies vary from 99.0 per cent at 60 cycles per second to 99.2 per cent at 600–1,200 cycles per second, and can be made higher if desired. In order to save on critical materials aluminum, instead of copper, is figured on in all of these designs.

Table II. Five Exploratory Compressed-Gas Designs for Power Transformers

	A	B	C	D	E
Rated output (in kilovolt-amperes)	20,000	20,000	20,000	50,000	100,000
Periodicity, N (cycles per second)	60	600	1,200	1,200	1,200
Primary voltage (kilovolts)	80	80	80	200	345
Primary turns (T)	240	160	132	180	200
Magnetic flux (M) (megalines)	126	18.8	11.4	21.0	32.5
Volts per turn	333	500	605	1,110	1,730
Space factor (in winding window)	0.158	0.121	0.110	0.087	0.054
Gross stacking height (inches)	80	65	58	80	100
Primary plus secondary I^2R losses at rated load. Power factor = 1.0 and 170 degrees centigrade (kilowatts)	142	77	54	103	168
Core loss (kilowatts)	42	62	73	184	430
Allowance for stray load loss plus ventilation (kilowatts)	10	15	25	50	75
Total losses, power factor = 1.0 (kilowatts)	194	154	152	337	673
Rated load efficiency, power factor = 1.0 (per cent)	99.0	99.2	99.2	99.3	99.3
Weights					
Laminations	63,000	15,500	12,100	30,600	72,000
Conductors (aluminum)	1,700	900	600	1,200	1,900
All else (including tank, ventilating equipment, insulation, and so forth)	19,300	4,600	3,700	9,200	21,800
Total weight	84,000	21,000	16,400	41,000	95,700
Total weight per kilovolt-ampere	4.2	1.05	0.82	0.82	0.96
Per kilovolt-ampere weight of					
Laminations	3.2	0.78	0.61	0.61	0.72
Conductors	0.085	0.045	0.030	0.024	0.019
Laminations plus conductors	3.3	0.82	0.64	0.63	0.74
Laminations plus conductors in per cent of total weight (per cent)	79	78	78	77	77

These exploratory designs were carried only far enough to disclose trends clearly, to ascertain and check the general consequences of employing periodicities of 600 and 1,200 cycles per second. Attention is called to the fact that the conductor is of aluminum.

References

1. Transmission of Power in Compressed-Gas Atmospheres. *Journal* Franklin Institute, September and October 1942, pages 251–73, 331–53.

2. The Effect of Temperature Upon the Hysteresis Loss in Sheet Steel, Malcolm MacLaren. AIEE Transactions, volume 30, 1911, pages 761–75; volume 31, 1912, pages 2025–35.

3. H. H. Skilling. *Electrical World*, February 15, 1936.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Pentode Ignitrons for Electronic Power Converters

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Synopsis: The name "pentode ignitron" has been applied to a high-voltage tube used for rectification and inversion in electronic converters. It is a sealed steel tube having an anode, three grids, and a mercury-pool cathode. The tube is of the gas-filled type with control characteristics similar to those of the thyratron. An ignitor establishes a cathode spot each cycle on the mercury pool. The cathode spot is stabilized at low anode currents by the use of an auxiliary holding anode. The functions of the three grids are somewhat similar to those in the pentode vacuum tube, but the manner of use is quite different. The first grid, or one nearest the anode, is used as an intermediate anode to divide potential gradients across the tube during nonconducting periods. The second, or center grid, is used to control the starting of the conduction period and the third, or outer grid, as a shield to permit rapid deionization of the space between the control and shield grids.

The introduction of multiple grids in ignitron tubes provides certain characteristics which are essential to inverter operation. It is the purpose of this paper to describe some of these characteristics, and the design and construction of the pentode ignitron tube.

These tubes are the ones installed in the power converter described in the companion papers.[1,6–8]

THE history and development of suitable electronic tubes and circuits for high-voltage power-conversion systems is discussed in a companion paper.[1] Briefly, the first power unit after laboratory tests consisted of a 40–60-cycle 5,000-kw power converter. The tubes for this unit were of the thyratron* type. The nonsynchronous link operated at a direct potential of 30,000 volts and a current of approximately 170 amperes. Power from the 40-cycle system was converted to direct current through 12 phanotron tubes connected in a cascade arrangement of two three-phase double-way rectifier circuits. The direct current was converted to 60-cycle a-c power through 12 thyratrons connected in a similar arrangement of double-way inverter circuits. Initial operation was remarkably free from any basic trouble. Operation over a period of some five years demonstrated the complete practicability of sealed electronic tubes for this type of service.

The development of the ignitor[2] opened the possibility of utilizing the control features of the thyratron design in a tube having the inherent high-current capacity of the mercury pool. Operating experience with sealed steel ignitrons[3] which were developed for service in the 250- to 600-volt power-conversion fields indicated the basic soundness of the ignitron principle of starting a new cathode spot each conducting cycle. These ignitrons are of sufficient current capacity to give power rectifiers of 75 to 1,000 kw, depending on voltage. Similar ignitrons operate in welding service with precise cycle-by-cycle control of currents from 1,000 to 10,000 amperes. The rectifier ignitrons were designed to have the lowest possible arc loss consistent with essentially complete freedom from arc-backs under normal operation conditions. The designs consist of an anode, a mercury-pool cathode, ignitors, and a small auxiliary anode for maintaining the cathode spot during the normal conducting period in case the main anode current falls below the value (about three amperes) at which the arc is stable. There is a minimum of shielding. Voltage control is obtained by shifting the phase angle at which the ignitor is fired. Variation in the starting or ignition time is small (of the order of 50 to 100 microseconds) with the usual excitation circuits.[4] Deionization time at the end of conduction is relatively long because of the open construction. Deionization time in an ignitron is the time required for the ionization in the space to decay sufficiently so that cathode spots are not reestablished if the anode is made positive. In a thyratron, it is the time required for ion sheaths to close the holes in the grids and effectively isolate the anode from the main body of the tube. Ignitron tubes have operated successfully as inverters,[5] but the required commutation time is in general too long to permit optimum operation. For such service, the grid-control characteristics of the thyratron tube for accurate starting and rapid deionization, in combination with the high current capacity of the mercury pool, seemed to offer attractive possibilities for the design of higher-voltage higher-current tubes.

Description

The pentode ignitron tube consists of an anode, a mercury pool cathode with ignitors and auxiliary anodes, and three grids. The similarity of the construction—an anode, three grids, and a cathode—to that of the pentode vacuum tube, suggested the use of pentode ignitron as a name. The general design is shown in Figures 1 and 2. The anode is surrounded by the grids. Starting from the anode, the first grid acts as an intermediate anode to divide potential gradients during nonconducting periods. The next or control grid determines the time of starting and reduces the deionization period at the end of conduction. The outer or shield grid acts in a dual capacity to pick up the arc at the beginning of conduction and to shield the rest of the grid–anode structure from residual ionization after conduction. The cathode has three ignitors, only one of which need be used at a time. There are also two auxiliary holding anodes. One auxiliary anode maintains cathode-spot excitation for low anode currents and the other is used for control purposes as described later.

The tube conversion capacity is of the

Table I

Maximum peak inverse or forward voltage, volts	20,000
Maximum anode current, amperes:	
Instantaneous	900
Average:	
Continuous	150
Two hours	200
One minute	300
Minimum water flow, gallons per minute	3
Minimum inlet water temperature, C	35
Maximum inlet water temperature, C	45

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* Thyratron is a generic term describing a gas-discharge thermionic-cathode tube in which one or more grids control the starting of the current electrostatically.



Figure 1. The pentode ignitron tube used in the 20,000-kw electronic power converter

order of 1,500 kw per tube. Companion papers[6–8] describe the installation and application of the pentode ignitrons in a 20,000-kw 25-to-60-cycle electronic power converter. The converter is divided into two 10,000-kw units. Each unit consists of two sections, a rectifier and an inverter, whose functions may be reversed, depending on the desired direction of power flow. There are 12 tubes in each section connected in two three-phase double-way rectifiers in cascade. The nonsynchronous d-c tie operates at approximately 30,000 volts and 340 amperes.

Development

The design of the tube is based on work by K. H. Kingdon and E. J. Lawton[9] on the cause of arc-back and on the possibility of increasing the operating voltage of gas-filled tubes through potential division.

A single-anode tube has a fairly definite maximum voltage limit at which arc-back occurs. This limit depends on load conditions, vapor pressure, and tube design. Reduction of potential gradients by means of potential dividing anodes would seem to offer the possibility of increasing the operating voltage. The principle has been successfully applied in high-vacuum rectifier tubes for X-ray service. In the case of gas-filled tubes, and particularly for tubes operating at relatively high currents, it is impracticable to divide the inverse potential at the end of commutation since the ion densities are too great. A few degrees later in phase angle, however, ionization has decreased to values which permit successful use of potential division.

Arc-back or inverse current conduction has been an interesting subject of discussion for many investigators.[10–12] The occurrence is definitely of a random nature, and the time may vary[13] from immediately after conduction when ion densities are greatest to any point in the inverse cycle. One obvious cause is too high vapor pressure due to lack of sufficient cooling or to mercury droplets striking hot objects and transiently increasing the vapor pressure above the voltage breakdown of the gas. The relation between vapor pressure and temperature for equilibrium conditions is shown in Figure 3. Mercury droplets are ejected from the surface of the pool by the action of the cathode spots. These droplets leave the pool with a random velocity so that the space above the pool and around the anode is filled with flying droplets during both the conducting and inverse parts of the cycle. These droplets do not always cause arc-back even when striking a hot anode, but it seems probable that a sufficient concentration of them may well do so.

As a result of tests on the influence of contaminating materials such as glass, quartz, asbestos, and so forth, on the rate of arc-back, the hypothesis has been

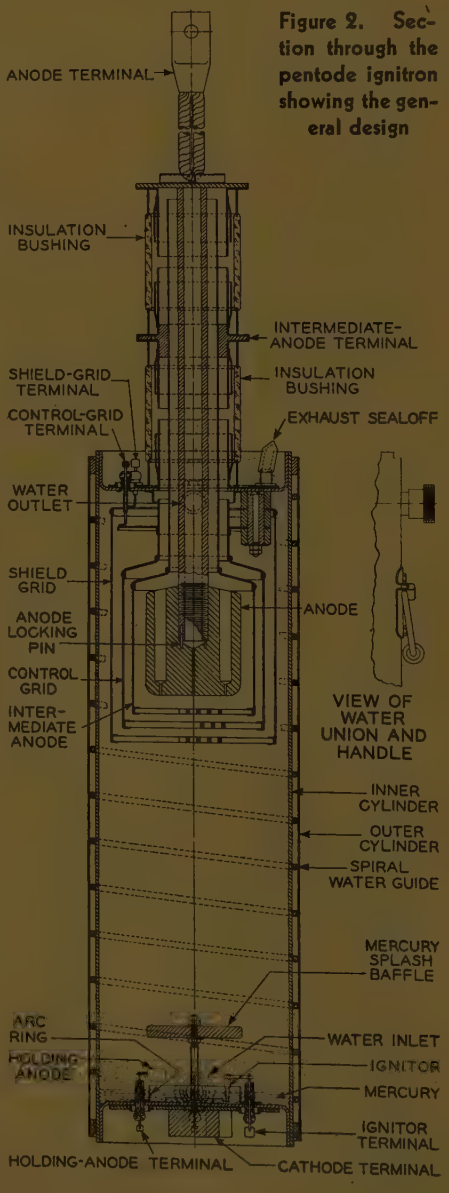


Figure 2. Section through the pentode ignitron showing the general design

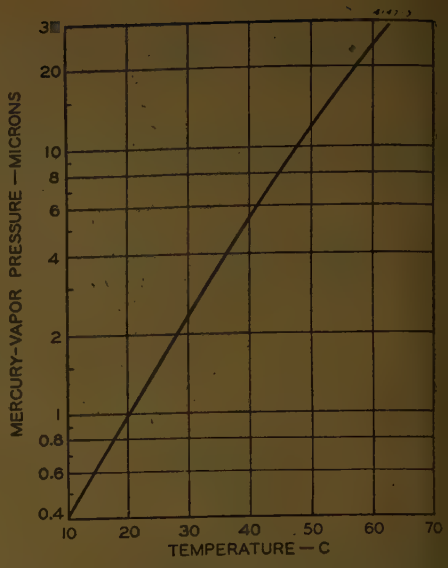


Figure 3. Relation between mercury-vapor pressure and temperature for equilibrium conditions

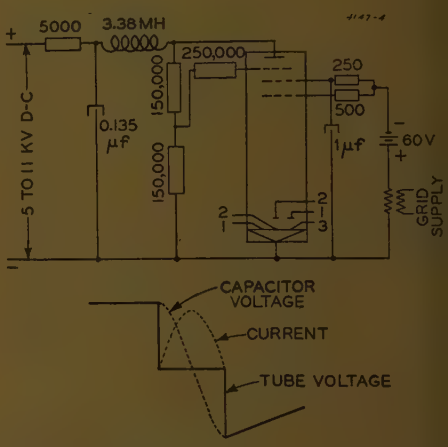


Figure 4. Single-tube inverter test circuit for investigating effect of circuit on arc-back rate

proposed by Doctor Kingdon that arc-backs are due basically to the collection of charges on small insulating patches on the anode. If the potential gradient exceeds the field emission requirements of the base material, arc-back results. Whether or not a sufficient potential gradient is established depends on the rate at which ions strike the insulating particle and the rate at which the charge leaks off. The rate at which ions strike the anode is proportional to the ion density in the space in front of the anode at the end of conduction, and the negative voltage to which the anode is driven. The density of ionization bears a direct relationship to the current, and at the end of conduction both the current and inverse voltage may be defined in terms of circuit parameters. Test results indicate that the rate of arc-back is proportional to the product of the initial inverse voltage and the rate of current change. For each design of tube, therefore, a rate can be determined in terms of an arc-back factor δ which will

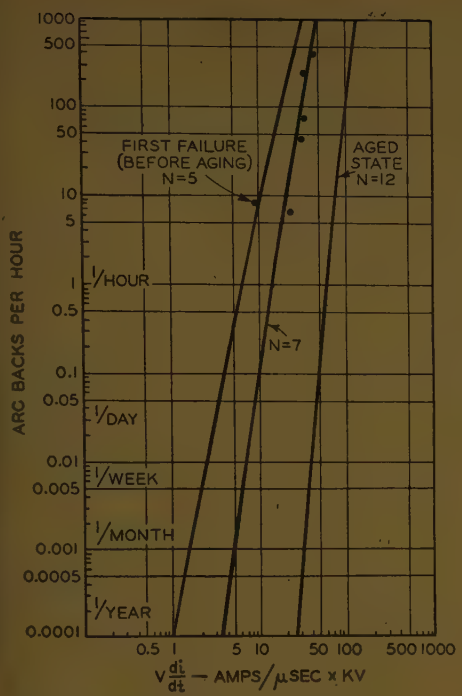


Figure 5. Relation between circuit arc-back factor $V(di/dt)$ and rate of arc-back for the conditions shown in Figure 4

permit its performance under other circuit conditions to be predicted. That is:

$$\delta = V\frac{di}{dt}$$

where

V = negative anode potential at end of commutation in kilovolts

$\frac{di}{dt}$ = rate of current change at the end of commutation in amperes per microsecond

Developmental tests under varying circuit conditions of current and voltage were made to determine the arc-back rate. The tests were made in a single-tube inverter circuit as shown in Figure 4. The duty was made sufficiently high to cause rapid failure (one to ten times per minute), and the data obtained are shown in Figure 5. The solid lines are a plot of the Poisson probability formula:

$$P(n) = \frac{e^{-\mu}\mu^n}{n!}$$

where

$P(n)$ = probability of arc-back (arc-backs per number of trials in unit time)

μ = average number of ions striking an area ΔA in time Δt. This is proportional to $V\frac{di}{dt}$

n = number of ions striking ΔA in Δt time for failure

With the proper choice of μ and n the curve may be made to fit the test data, and the probable values of arc-back rates for more normal circuit constants determined from its extrapolation.

Arc-back tests were also made under actual power conditions in a biphase single-way rectifier–inverter circuit shown in Figure 6. The transformer capacity was of the order of 1,000 kw and the arc-back factor 1.5–2 for no phase retard. At 25,000 volts peak inverse, 150 amperes current, a limit of 60 degrees centigrade (24½ microns) outlet water temperature was found. At 12,500 volts, 300 amperes, the temperature was 71 degrees centigrade (50 microns). These extremely high limits tend to reflect the gas-breakdown point for a particular pressure and spacing.

Considerable experience and data are being accumulated to substantiate the validity of the insulating-patch theory

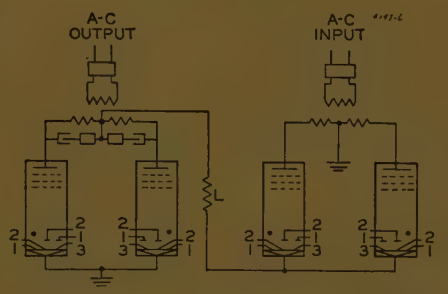


Figure 6. A 1,000-kw operation-test circuit for rectification and inversion

and the definition of circuit duty in terms of an arc-back factor. Correlation between test conditions and actual performance in power circuits is sufficiently close to indicate the importance of the concept in understanding the mechanism of arc-back.

Control measurements were taken with a rotary switch in the circuit shown in Figure 7. In this case, a quarter-wave of current is passed through the tube under test. At the peak of the wave, the current is rapidly commutated to a second tube. Shortly after this, the mechanical switch opens and at a given interval later, a positive voltage is applied to the anode of the tube under test. Its ability to withstand this voltage is a measure of its deionization time, that is, its ability to regain control. Figure 8 shows the test results. The effect of gas pressure and grid-circuit impedance is clearly indicated.

Construction

The pentode ignitron is arranged for water cooling, that is, the mercury-vapor pressure is controlled by circulating water of the proper temperature through a water jacket surrounding the tube walls. A spiral water guide imparts a velocity of approximately five feet per second to the water to insure efficient heat transfer. An adequate amount of mercury-condensing surface is provided in the tube to maintain a low vapor pressure during overloads and at the same time allow for a reasonable variation of cooling-water temperature. Sudden increases in vapor pressure due to flying mercury droplets hitting hot anode and grid surfaces are prevented by the addition of a graphite splash baffle. The effects of ionization and vapor pressure are further controlled by suitable shielding in the anode stem and by the action of the grids.

Electrode connections are brought out through permanently vacuum-tight insulation bushings consisting of high-resistance borosilicate glass sealed to fernico.

All water-cooled surfaces of this tube are made of stainless steel which is highly resistant to corrosion. The inner cylinder is formed out of sheet material and a

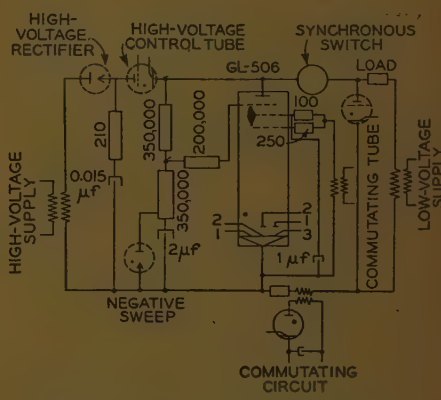
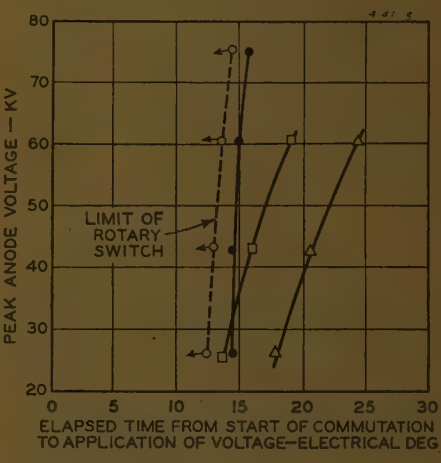


Figure 7. Kingdon–Lawton rotary-switch circuit for determining the recovery of grid control following commutation



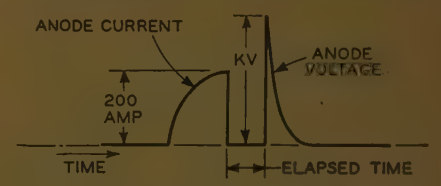


Figure 8. Grid-control recovery characteristics following commutation

△ 60 C_j control-grid resistance 500 ohms
□ 60 C_j control-grid resistance 100 ohms
○ 40 C_j control-grid resistance 100 or 250 ohms
● 40 C_j control-grid resistance 500 ohms

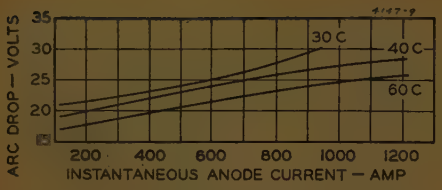


Figure 9. Arc-drop characteristics for cooling-water temperature of 30–60 degrees centigrade

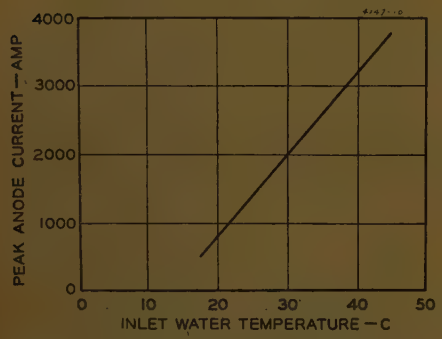


Figure 10. Effect of cooling-water temperatures on tube fault-current capacity

vacuum-tight seam weld is made by arc welding. After the anode and grids are properly assembled on the anode header, it is set into one end of the cylinder and a vacuum-tight weld made. The cathode header is similarly assembled and welded to the other end of the cylinder to complete the vacuum envelope of the tube.

At this point the tube receives a very rigorous degassing treatment during which the mercury is introduced and at the end of which it is permanently sealed off from the pumping system.

The outer cylinder, or water jacket, is formed from sheet material similar to the inner cylinder and its seam is fusion welded. After the water nipples are welded to it, the outer cylinder is slid over the tube and the ends are welded to the spacer rings.

Characteristics and Ratings

The general sequence of operation of the electrodes in the pentode ignitron is as follows. A cathode spot is established for each conducting cycle by means of one of the ignitors. Auxiliary anode current starts as soon as the ignitor fires. The shield and control grids are then made positive, and conduction is established first to the shield grid, then the control and finally to the anode. Phase control is obtained, as described in a companion paper[7] by simultaneously shifting the ignitor, holding anode, and grid voltages; or the grid voltage may be shifted with respect to the ignitor.

The introduction of multiple grids in the tube design considerably influences the tube characteristics. The arc drop is higher than that which is obtained in the lower voltage ignitrons. Figure 9 shows this characteristic. Some of the increase in arc drop is due to the length of arc path below the grids themselves. The arc drop plotted is the average value over the conducting period, that is, the arc-drop wave shape of gas-filled tubes is, in general, higher at the beginning of the period when there is a deficiency of ions and vapor and lower at the end of the conducting period. These values may vary from the mean by one to two volts. For the data in Figure 9, the tubes were operated in a three-phase single-way circuit with resistance load. The arc drop was measured by means of a cathode-ray oscilloscope and amplifier with an inverse-voltage blocking circuit. The abscissa is the maximum or peak of the current carried.

The amount of fault current which the tube can conduct is shown in Figure 10. This characteristic is reduced by the introduction of multiple grids. Two distinct phenomena occur depending on the mercury-vapor pressure. At low pressures corresponding to about 20 degrees centigrade, there is a tendency for surges

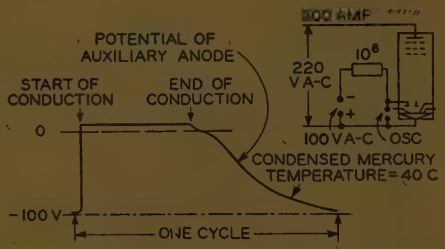


Figure 11. Potential of a probe near the cathode used to indicate ion density after conduction

if the current exceeds to any appreciable extent the value indicated. This phenomena is due to so-called "starvation" of the arc. In effect, all of the gas molecules in the grid openings are ionized and swept out of the immediate region before new molecules can diffuse in from the gas outside. The current tends to cease abruptly for an instant and this rate of current change, in connection with circuit inductances associated with the current path, generates high voltages. It is common practice in mercury-arc equipment to protect associated transformers with suitable resistance-capacitance filters or Thyrite.

At higher vapor pressures, corresponding to temperatures of 40 degrees centigrade and above, and higher currents, the arc drop tends to reach a value where cathode spots may form spontaneously on the walls of the tube. If this happens too frequently, sufficient metal may be vaporized into the mercury pool to cause wetting of the ignitors. Higher vapor pressures reduce the arc drop and increase the fault-current capacity, and it is desirable to operate the tube at as high a vapor pressure as is consistent with freedom from arc-back or loss of control. The pentode ignitron tube was designed to operate at the higher temperatures in order to minimize water-cooling and heat-exchanger problems. As a result, however, operation at low temperatures is limited.

In gas-filled tubes having relatively large open volumes, ionization is present in this space during all of the inverse cycle. It decays rapidly in magnitude following the cessation of anode-current conduction. Figure 11 shows a drawing of a cathode-ray oscilloscope trace of the voltage on one of the auxiliary anodes used as a probe. The anode was connected through a one-megohm resistance to a 100-volt negative bias. The voltage trace shows clearly the rate of ionization decay and the need for the shield-grid construction.

The grid-control characteristics are somewhat different from those of the usual thyratron. The "turn on" or firing conditions are essentially independent of the anode voltage in the range of 1,000 to 30,000 volts. Both grids must be made positive to establish conduction. If either is negative, the tube will not conduct. Conduction must first be established to the shield grid and then to the control grid. The optimum point for this occurs at the peak of the ignition-current impulse when an ionization density equivalent to the 40- to 50-ampere ignitor current is reached. Beyond this point, the ionization corresponds to that of the auxiliary-anode current and slightly higher shield-grid voltages are required. Under ordinary conditions, the shield-grid potential to establish conduction is of the order of 15 to 30 volts. Control-grid conduction voltages are correspondingly low. Figure 12 shows the grid-circuit control system used in the operation test, Figure 6.

The essential ratings defining the tube characteristics are tabulated in Table I.

Application

The application of electronic tubes involves careful co-ordination of the tube characteristics with the circuit requirements. The application engineer is concerned primarily with the over-all performance of the apparatus. The tube engineer must be concerned with the requirements as the tube sees them. While it is necessary to consider the average and rms voltages and currents, it is mandatory to consider the instantaneous.

Figures 13 and 14 show the anode-current and anode-to-cathode-voltage wave shapes for a tube in a three-phase double-way rectifier and in an inverter. The voltage traces are the logical result of rectifier and inverter operation in a three-phase system. In the case of the rectifier, the anode voltage is driven sharply negative at the instant the current transfer is complete. The resultant arc-back factor $V(di/dt)$ and the stress on the tube will be high when the voltage is high. It is in-

Design and Operating Characteristics of Modern Dry-Type Air-Cooled Transformers

W. W. SATTERLEE
MEMBER AIEE

A DRY-TYPE transformer may be defined as a transformer devoid of all liquids and one whose core and coils and associated parts are cooled by the natural or forced circulation of air.

Dry-type air-cooled transformers are not new; in fact, the first transformer ever built was of dry-type design. However, until a relatively recent date, dry-type transformers have been usually of small kilovolt-ampere rating and low voltage, or of relatively large size of the air-blast type. These transformers have been constructed with organic insulating substances, known as class *A* insulation. Class *A* insulation is not fireproof.

In 1936, a new kind of dry-type transformer was developed. These transformers are now built in ratings as large as 4,000 kva and they are cooled simply by the natural circulation of air through their core and coil structures. With forced-air cooling the capacity of these transformers may be increased materially.

These dry-type transformers are explosion-proof and essentially fireproof because their major insulation is principally air and porcelain. In fact, their insulation structures are made up largely of inorganic substances, such as porcelain, glass, asbestos, and mica, or combinations of these and other substances, bonded together with a heat-resisting varnish. Combinations of these substances are now defined in American Standard *C*-57.1 as class *B* insulation.

Temperature Limitations

Briefly stated, the present definition of class *B* insulation covers inorganic substances such as mica, asbestos, and fiber glass in built-up form with organic binding substances. A small portion of class *A* materials may be used for structural purposes provided the electrical and mechanical properties of the insulating structure are not impaired by a continuous temperature of 130 degrees centigrade.

Although the continuous temperature limit specified for class *B* insulation was established some time ago at 130 degrees centigrade it is obvious from the definition of class *B* insulation that there might be different combinations of organic and inorganic substances that would have temperature limits varying over a considerable temperature range. This is further indicated in AIEE Standards number 1 by the statement: "Materially higher 'hottest-spot' temperatures may be satisfactory for particular forms of class *B* insulation, . . ." Consequently, it has been recognized that certain combinations of these substances will withstand higher operating temperatures than 130 degrees centigrade. Such a combination of insulating materials has been used in developing the dry-type transformer described in this paper.

Now, let us consider just how these insulating materials are used in the construction of these transformers so that they will give satisfactory service over long periods of time even though under sustained rated load their "hottest-spot" copper temperatures may be in excess of 130 degrees centigrade.

Figure 1 is a photograph showing the structural detail of the type of winding commonly used in these transformers for operating voltages up to 5,000. Figure 2 shows the type of winding used for voltages above 5,000 and up to and including 15,000.

Winding conductors are insulated with glass or asbestos. Sections of the high- and low-voltage windings are separated by porcelain, mica, or asbestos. High-voltage windings are supported on porcelain. Porcelain is used to center the high- and low-voltage winding assembly on the core. Porcelain spacers form large

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The author acknowledges the valuable assistance relative to the measurement of temperatures received from Paul Narbut of the small power and network transformer division of the Westinghouse Electric and Manufacturing Company, Sharon, Pa.

Table I. Insulating-Barrier Hottest-Spot Temperature Rise Expressed as Per Cent of the Hottest-Spot Copper Temperature Rise

Duct Length, Inches	Duct Width		
	1/2 Inch	3/4 Inch	1 1/2 Inches
15	58	52	52
30	67	58	52
45	73	62	52

consideration of the series motor and perhaps even in the consideration of a shunt type of motor in which the armature reaction very seriously affects the field distribution. However it is true that for motors of the aircraft type it probably will be found that under the normal operating conditions the strength of the series field is so great as substantially to saturate the magnetic structure of the motor and to produce conditions more nearly like those proposed in this discussion than might appear at first glance. It is probably also true that there is enough similarity between a series motor and a shunt motor to make the end result of an individual study of each reach approximately the same conclusion.

The procedure outlined in the paper has not included the study of the field structure of the machine, although, actually, work which already has been carried out did include the field structure in order to complete the story and obtain an accurate picture of the output per pound of active material in the motor.

It is believed that the conclusions which have been reached in connection with the study should make it possible to compare motors of this type which differ greatly from one another in size more accurately than by the more conventional D^2L method of comparing designs.

General Comments

As mentioned originally in connection with the comments on proportions of mechanical structures, there are factors which dictate that as the size of an electrical machine is increased indefinitely its proportions must change. For this reason, a study of machine effectiveness made over a considerable range in size must be accompanied by a review at intervals of the design limitations to make certain that a factor which was important in a small machine does not become negligible in a large machine or vice versa.

One important factor which was omitted purposely from the present study was the effect of iron loss. Such loss will be seen to vary in respect to the output as the output is increased while the flux density is maintained constant; because with flux density constant the iron loss will vary as the first power of the weight, whereas the output has been shown to vary as the 5/8 power of the weight. With two similar machines differing in size the iron loss of the large machine should be less important in comparison with the output.

air-ducts between sections of the high-voltage winding and between the low-voltage winding and the core. A clear cylindrical duct of air separates the low-voltage winding from the high-voltage winding. Dirt which falls into this high-potential area simply drops through to the bottom of the transformer case. Horizontal creepage surfaces in high-potential areas are eliminated. High-voltage and low-voltage leads are supported on or terminated in porcelain insulators.

Figure 3 shows a cross section of a low-voltage and high-voltage winding and the insulation structure. The windings are individually treated with a heat-resisting thermoset varnish and baked at a high temperature. The core and winding assemblies are also treated with the same heat-resisting varnish and baked at a high temperature which eliminates the residual volatile matter. This treatment imparts a smooth, tough, glossy finish to the complete structure which makes it highly resistant to heat, dirt, and moisture.

Glass, an inorganic substance, is highly resistant to temperature. It is also essentially nonhygroscopic. However, in order to insulate copper suitably with glass for use as a winding conductor, it is necessary to bond the glass fibers and filaments mechanically to the copper. This bond imparts the mechanical stability required to use the insulated conductor in winding coils and assembling them into transformers. Tests made on windings treated with this bond show that it will withstand continuous temperatures of the order of 160 degrees centigrade for long periods of time with but little loss of its mechanical cementing properties. The insulation is not materially affected by an amount of moisture that may be expected under operating conditions. Operating experience has confirmed that it is not necessary for the varnish to retain its original properties fully during the operating life of the transformers, as will be shown later.



Figure 1. Structural detail of a barrel-type low-voltage winding



Figure 2. Structural detail of a disk-type high-voltage winding

Temperature Distribution

Let us now consider what the operating temperatures of the windings and the insulation structures may be and how the transformers are assembled to withstand these temperatures.

According to American Standard *C*-57.1, dry-type transformers constructed with class *B* insulation (combinations of organic and inorganic substances) are permitted to operate at a continuous average copper temperature rise of 80 degrees centigrade, measured by the resistance method. With the present maximum permissible temperature of 130 degrees centigrade for class *B* insulation and with an ambient temperature of 40 degrees centigrade the hottest-spot copper allowance in a transformer would be only 10 degrees centigrade. This seems inconsistent because even in properly designed dry-type transformers of small kilovolt-ampere rating, aside from those of large kilovolt-ampere rating, relatively high temperature gradients may exist and it is not practicable to limit the hottest-spot allowance to 10 degrees centigrade.

If we consider the actual temperatures to which the insulation of a dry-type transformer may be subjected, it is necessary to recognize the existence of temperature gradients within the transformer itself. The insulation nearest the winding copper is necessarily subjected to higher temperatures than the insulation physically removed from the copper. Thus the turn insulation, directly in thermal contact with the copper, is, for all practical purposes, subjected to the same temperatures as the copper. On the other hand, insulating barriers which are not in thermal contact with the copper will be at appreciably lower temperatures than the windings.

To obtain data on temperature gradients within dry-type transformers, tests have been made on model winding assemblies, representative of small as well as large transformers, and on actual transformers, to determine (a) copper temperatures and their distribution in the windings of transformers as representative of the insulation in direct contact with the copper, and (b) temperatures of the insulating barriers in transformers as related to the copper temperatures and the space between the copper and the barriers.

Figure 4 shows a model winding assembly on which a series of tests were made by passing a certain current through the winding until steady temperature conditions were obtained and recorded by thermocouple readings. Thermocouples were

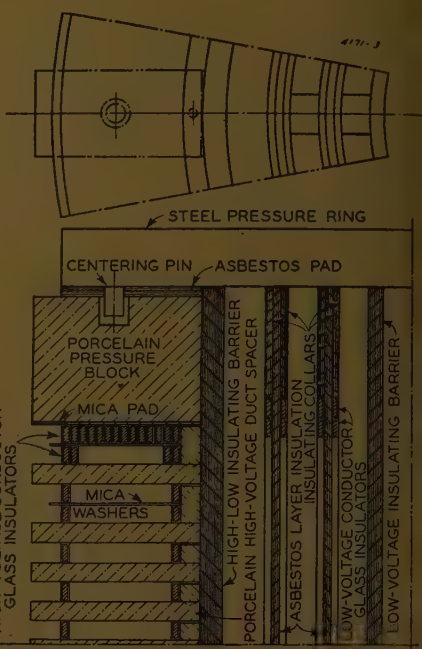


Figure 3. Cross section of the low-voltage and high-voltage winding and the insulation structure

used because they afford the most suitable experimental means of measuring local temperatures.

The winding assembly consisted of three sections, each section 15 inches long. The three sections could be used either separately or stacked one upon the other, thus giving three effective winding lengths. The width of the cooling duct separating the winding from the insulating barrier was made variable by using barriers of different diameters. Thermocouple junctions were soldered to the copper conductors. A three-quarter inch retaining collar at the ends of each section reduced the effective length of the winding assembly to $13\frac{1}{2}$ inches per section.

In all these tests, it was found that the hottest-spot copper temperature is located at a distance of 10 to 15 per cent below the top of the winding, the higher value applying to the wider ducts. For any specific duct width and length, the hottest-spot copper temperature rise was found to be proportional to the average copper temperature rise, thus giving a constant value for the ratio of the

hottest-spot copper temperature rise to the average copper temperature rise. This ratio was found to vary with the width and length of the air duct separating the winding from the insulating barrier. This is shown in Figure 5. The highest value was 1.36 for the longest and narrowest duct.

Due to the presence of a radial gradient and a reduced radiation at the ends of the winding because of the retaining collars, the ratio of the hottest-spot copper temperature rise to the average copper temperature rise may be expected to be greater in the windings of actual transformers when compared with that obtained in the model winding.

To determine the location of the hottest-spot temperature in the windings of actual transformers, a series of tests was made on a 300-kva 2,400-volt single-phase transformer, and on several 500- to 800-kva 13,800-volt three-phase transformers. All of these transformers had a pancake or disk-type outer high-voltage winding with three-eighths-inch air ducts separating the disk sections. A three-eighths-inch air duct separated the inside of the winding from the outside of its insulating barrier. The inner or low-voltage windings were of the cylindrical or barrel type of two- and four-layer construction having air ducts from five-eighths inch to seven-eighths inch wide, separating the layers from each other, and from the inner and outer insulating barriers.

The 300-kva transformer had a number of thermocouples placed in the low-voltage and high-voltage windings, the object being, primarily, to locate the hottest spot. Tests made on this transformer show that for the same average temperature in each winding, the upper portion of the inner or low-voltage winding is considerably hotter than the hottest part of the outer or high-voltage winding, thus definitely locating the hottest-spot of the assembly in the upper part of the inner winding. A study of the radial temperature distribution in the inner winding shows the existence of a temperature gradient from layer to layer with the highest temperature in the outer layer.

The highest value of the ratio of the hottest-spot copper temperature rise to the average copper temperature rise recorded for natural draft operation on the 500- and 800-kva transformers, was 1.41. With forced-air operation of these transformers and with the same average copper temperature rise, the value of this ratio, in general, may tend to be somewhat higher. In particular, this may be due to the more turbulent action of the air which exists with high air velocities. In an extreme case, however, a high value of the ratio of the hottest-spot copper temperature rise to the average copper temperature rise may be traced to an unequal distribution of the air flow between the phases of the transformer and a consequent inequality of the average copper temperature in the different phases.

A comparison of actual copper and barrier temperatures obtained is as follows: Based on an average temperature rise of the windings of 80 degrees centigrade as determined by the resistance method, the hottest-spot copper temperature rise will range from approximately 100 to 115 degrees centigrade. With

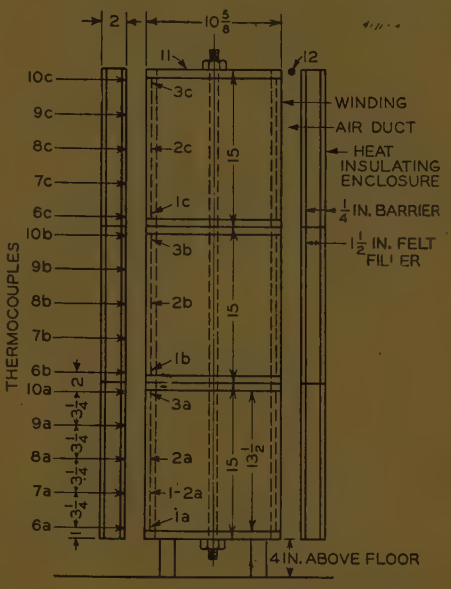


Figure 4. Model winding and barrier assembly used for temperature distribution tests

an ambient temperature of 40 degrees centigrade the hottest-spot temperature of the copper will range from approximately 140 to 155 degrees centigrade.

Data obtained on the model winding assembly, Figure 4, show temperatures on the surface of the insulating barrier adjacent to the winding but separated from it by air ducts of variable widths. Figure 6 shows a typical temperature distribution in the winding and the barrier with a one-half-inch-wide air duct separating the winding and the barrier. Expressed as a fraction of the hottest-spot copper temperature rise, the value of the barrier hottest-spot temperature rise varies from 0.58 to 0.73.

It has been shown that the barrier insulation in these transformers will operate at appreciably lower temperatures than those of the windings. See Table I and Figure 6. From these data, it may be noted that the hottest-spot temperature of the barrier insulation in properly designed dry-type transformers having an 80-degree centigrade average winding temperature rise as measured by the resistance method will be approximately 115 degrees centigrade; that is, it will range from 25 to 40 degrees centigrade below the hottest-spot winding temperature. Life tests on the barrier insulation used in these transformers show that it will withstand a continuous temperature of 130 degrees centigrade without detrimentally affecting its electrical and mechanical strength.

It is important to note that none of the insulation which may reach a temperature in excess of 130 degrees centigrade is subjected to line-to-line or line-to-ground voltage stresses. The voltage where temperatures above 130 degrees centigrade may occur is limited to winding turn-to-turn stresses which do not exceed 80 volts.

Service Tests

A 500-kva 13,800-volt three-phase dry-type transformer was moved outdoors and given a rain test for 16 days with a water spray having a precipitation of 0.2 inch per minute applied continuously around the case. The case had screened openings in the bottom and the top. The transformer carried no load but was excited at 13,800 volts for 16 out of each 24 hours. During the period without excitation, the rain test was continued.

After this extreme test, lasting for 16 days, the transformer was subjected to the standard dielectric test of 31,000 volts from the high-voltage winding to the low-voltage winding and to ground for 60 seconds with no signs of distress whatever. While these dry-type transformers are intended only for indoor application where very little moisture is likely to be present, these results are reassuring when condensation or the effects of high humidity during installation and operation are considered.

A 150-kva 2,400-volt three-phase dry-type transformer, enclosed by an open-screened case, has been under test for more than 20 months, supplying power for the operation of motors and other equipment. The load cycle begins each week at 8:00 a.m. on Monday. The test is conducted in a relative humidity of approximately 95 per cent and at a room temperature varying from 32 to 50 degrees centigrade. At 4:00 p.m. each day, through Monday to Saturday, the load is removed but the transformer is kept energized and left to "soak" overnight. From 4:00 p.m. on Saturday until 8:00 a.m. on Monday, the transformer remains energized and the 95 per cent relative humidity is maintained. Under these extreme conditions this transformer has given continuous service and there are no indications of a deterioration of the varnish or of the insulation. The test is being continued.

Another dry-type transformer, enclosed by a sheet-steel case with louvers, having a normal rating of 480 volts, has been subjected to temperature and moisture tests for more than 2,000 hours, operating under the following cycle. The load was adjusted so that the average maximum continuous winding tempera-

ture, determined by thermocouples, is 175 degrees centigrade. The load is maintained for 16 of each 24 hours during five days of each week. At the end of each 16-hour load period, a water spray is directed against the transformer case for five minutes. Following this, the transformer is allowed to stand eight hours of each day without power applied. At the end of each weekly cycle, immediately following the last 16-hour load period, the standard dielectric test of 4,000 volts is made from the high-voltage winding to the low-voltage winding and to ground. Following this test, the water spray is applied for five minutes. The transformer then remains de-energized for 56 hours when power is again

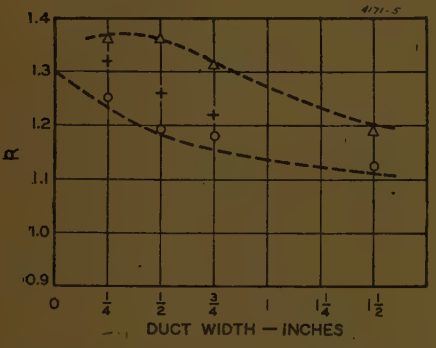


Figure 5. Variation of ratio of temperature rises

$$R = \frac{\text{hottest-spot temperature rise}}{\text{average temperature rise}}$$

○—Duct length 15 inches
+—Duct length 30 inches
△—Duct length 45 inches

applied and the weekly test cycle is repeated.

A careful examination of the transformer after the 2,000-hour test period disclosed some deterioration of the varnish but no insulation failures have occurred nor were there any indications of a decrease in the dielectric strength of the insulation. The test is being continued.

Operating Experience

Many transformers of this type have been in continuous operation for more than seven years. Today, there are nearly 2,000 transformers in service totaling approximately 1,000,000 kva. Never has a breakdown of the winding insulation occurred in any of these transformers which could be directly attributed to an excessive hot-spot winding temperature, an accumulation of dirt, the effects of condensation, or operation in a humid atmosphere.

With nearly 2,000 transformers in operation, four transformers have failed in service. It is of interest to note the cause of these failures, the resulting damage to the windings of the transformers, and what was necessary to put the transformers back into service.

A 600-kva three-phase 6,900-volt dry-type transformer flashed over as the result of a severe lightning surge. The voltage impressed on the high-voltage winding was estimated to be in excess of 100 kv. Connected directly to the secondary of a large bank of power transformers, this dry-type transformer had no lightning protection of any kind. The high-voltage winding was severely damaged; the line leads and a considerable portion of the winding were fused because the system breaker permitted arcing for a considerable period of time. It is interesting to know that neither an explosion nor a fire occurred. It was necessary to replace the windings.

A 1,000-kva three-phase 13,800-volt dry-type transformer was temporarily put out of service because of a lightning surge. However, in this case, flashover occurred across two plain rectangular

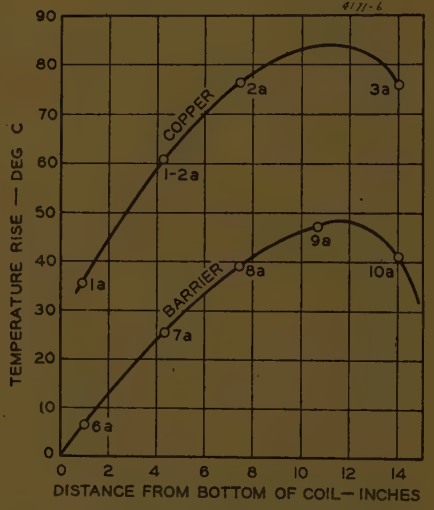


Figure 6. Typical curve of the temperature distribution in one section of a model winding assembly, showing the relation between the winding and the barrier temperatures for a one-half-inch duct width

porcelain insulators which were used to support the high-voltage leads. As a secondary result, a small arc occurred at one point between the two outer turns of the top high-voltage coil section of one phase. The turn insulation was only slightly damaged. Permanent repair of the winding was made and the transformer was put back into service within two hours after the failure. Later, the rectangular-shaped porcelain insulators were replaced with round corrugated insulators.

Two other dry-type transformers failed after short periods of service. One failure was caused by a short-circuit between two adjacent turns in the high voltage winding, apparently the result of a poor joint which punctured the insulation. Repair was effected easily and quickly. The other transformer outage was caused by the flashover of a porcelain lead support which may have been due to an excessive accumulation of conducting dirt. The only repair necessary was to replace the lead support.

There have been instances where a number of these dry-type transformers operated at overloads ranging from 150 to 200 per cent of rated load for continuous periods of time in excess of 24 hours. The windings must have been subjected to temperatures appreciably in excess of 160 degrees centigrade.

For instance, a bank of 150-kva 4,000-volt transformers carried in excess of 150 per cent of rated load for a period of nearly three days. This overload occurred nearly four years ago. A careful inspection of these transformers did not indicate any noticeable deterioration of the insulation. These transformers have continued to give uninterrupted service.

Two years ago, another bank of dry-type transformers carried approximately double load for more than 24 hours. These transformers have continued to operate without any ill effects of this heavy overload.

A 600-kva 13,800-volt three-phase dry-type transformer, after having been in service over one year, was operated at 200 per cent of rated load for a period of 30 hours. It is estimated that the maximum temperature of the windings was in excess of 225 degrees centigrade. No failure occurred and the transformer is continuing in service.

Conclusions

Because the major insulation of these dry-type transformers is primarily air it is unaffected by temperatures within the operating range.

There are available special winding conductor insulations which come within the scope of the present definition of class *B* insulation. These conductor insulations will withstand satisfactorily hottest-spot temperatures in excess of 130 degrees centigrade. Other parts of the insulation structure are subject to temperatures which are well within the established limits for the materials used.

Experience with dry-type transformers of this type operating under extremely humid conditions show that the effects of moisture are not a limiting factor.

Tests on these transformers, together with an operating record of more than seven years, give ample proof that these modern dry-type air-cooled transformers operating under normal as well as the usual abnormal conditions of service, may be expected to give continuous reliable service.

Bibliography of Relay Literature 1940–1943

AIEE RELAY SUBCOMMITTEE

Working Group on Relay Bibliography

THIS interim report was prepared by the working group on bibliography of the relay subcommittee of the AIEE committee on protective devices, in order to make immediately available this material to the entire industry. Previously it has been available only to the relay subcommittee in annual reports and then published at ten-year intervals. However, it was felt that the urgency of the relay problems brought about by the war emergency makes it desirable to have this information more generally available.

A "Bibliography of Relay Literature, 1927–1939" was published in pamphlet form in July 1941 and in the AIEE TRANSACTIONS, Volume 60, 1941, pages 1435–47.

The articles listed in this bibliography (1940 to 1943) include all those appearing in the AIEE TRANSACTIONS or ELECTRICAL ENGINEERING as well as in most of the chief technical publications of the world from January 1940 through December 1943. These articles are subdivided into several sections. In each section the entries are numbered consecutively and listed alphabetically by years, the first significant word of the title determining the alphabetical position.

Section A. Line Protection—Distance

1942

1. DISTANCE PROTECTION, F. J. Lane. *Electric Times* (London, England), volume 102, August 20, 1942, pages 256–9; August 27, 1942, pages 296–9.

1943

2. DISTANCE RELAY PROTECTION FOR SUBTRANSMISSION LINES MADE ECONOMICAL, L. J. Audlin, A. R. van C. Warrington. AIEE TRANSACTIONS, volume 62, 1943, September section, pages 574–8.

3. DISTANCE RELAYING WITH LOW-TENSION POTENTIALS, J. L. Blackburn. *Westinghouse Engineer*, volume 3, May 1943, pages 61–4.

4. PERFORMANCE REQUIREMENTS FOR RELAYS ON UNUSUALLY LONG TRANSMISSION LINES, F. C. Poage, C. A. Striefus, D. M. MacGregor, E. E. George. AIEE TRANSACTIONS, volume 62, 1943, June section, pages 257–83. Discussion, page 422.

5. PROBLEM—SOLUTION—TESTS IN RELAYING 270-MILE 154-KV LINE. *Electric Light and Power*, volume 21, February 1943, pages 36–41.

6. PROTECTIVE RELAYING FOR LONG TRANSMISSION LINES, A. R. van C. Warrington. AIEE TRANSACTIONS, volume 62, 1943, June section, pages 261–8. Discussion, page 427.

Paper 44-104, recommended by the AIEE committee on protective devices for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 10, 1944; made available for printing May 10, 1944.

Personnel of working group preparing this interim report: C. E. Parks, H. F. Lindemuth, S. Goldsmith, C. L. Smith, George Steeb.

Section B. Line Protection—Pilot Wire and Carrier Current

1940

1. CARRIER-CURRENT LOSSES MEASURED AND INTERFERENCE MINIMIZED ON BOULDER DAM–LOS ANGELES TRANSMISSION LINES, J. D. Laughlin, W. E. Pakala, M. E. Reagan. AIEE TRANSACTIONS, volume 59, 1940, January section, pages 4–9; discussion, page 9.

2. CARRIER-CURRENT RELAYING IMPROVED, S. L. Goldsborough. *Electrical World*, volume 113, January 1940, page 156.

1941

3. AN IMPROVED A-C PILOT-WIRE RELAY, J. H. Neher, A. J. McConnell. AIEE TRANSACTIONS, volume 60, 1941, January section, pages 12–17. Discussion, page 638.

4. POSSIBILITIES AND METHODS OF EXTENDING CARRIER-CURRENT RELAY CHANNEL TO OTHER USES, R. M. Smith, S. L. Goldsborough. AIEE TRANSACTIONS, volume 60, 1941, January section, pages 23–7. Discussion, page 645.

5. POWERTON–CRAWFORD 220-KV LINE—SYSTEM OPERATING FEATURES AND TERMINAL DESIGN, H. E. Wulfing, T. G. LeClair. AIEE TRANSACTIONS, volume 60, 1941, pages 1084–9; discussion, page 1304.

6. SUPERVISORY CIRCUIT CHECKS RELAY SYSTEM, R. M. Smith. *Electrical World*, volume 115, May 3, 1941, page 1507.

7. SUPERVISORY CIRCUIT PERFORMS DOUBLE DUTY, M. A. Bostwick. *Electrical World*, volume 115, June 28, 1941, page 2236.

1942

8. CARRIER-CURRENT RELAYING AND COMMUNICATION ON THE TVA SYSTEM, M. S. Merritt, T. D. Talmage. ELECTRICAL ENGINEERING, volume 61, November 1942, pages 561–72.

9. CARRIER-CURRENT SOLVES TVA RELAYING AND COMMUNICATIONS PROBLEMS, M. S. Merritt, T. D. Talmage. *Electric Light and Power*, volume 20, September 1942, pages 48–57.

10. MULTICHANNEL CARRIER-CURRENT FACILITIES FOR POWER LINE, P. N. Sandstrom, G. E. Foster. AIEE TRANSACTIONS, volume 61, 1942, February section, pages 71–6. Discussion, page 469.

11. PROTECTION OF PILOT-WIRE CIRCUITS, E. L. Harder, M. A. Bostwick. AIEE TRANSACTIONS, volume 61, 1942, September section, pages 645–51. Discussion, page 996.

12. RELAY AIDS USE OF 3-TERMINAL LINES. *Electrical World*, volume 118, August 22, 1942, page 638.

13. STAGED FAULT TESTS ON 154-KV LINE. *Electrical West*, volume 118, November 21, 1942, page 1708.

1943

14. A NEW-TYPE CARRIER RELAY PROTECTION E. M. Wood, H. W. Haberl, F. A. A. Baily. AIEE TRANSACTIONS, volume 62, 1943, December section, pages 774–9.

15. PILOT-WIRE CIRCUITS FOR PROTECTIVE RELAYING—EXPERIENCE AND PRACTICE, AIEE committee on protective devices. AIEE TRANSACTIONS, volume 62, 1943, May section, pages 210–14. Discussion, page 380.

16. PILOT-WIRE RELAYING ON A METROPOLITAN SYSTEM, T. G. LeClair, E. L. Michelson. AIEE TRANSACTIONS, volume 62, 1943, August section, pages 511–15.

17. PROTECTION OF THREE-TERMINAL LINES, M. A. Bostwick, E. L. Harder. *Westinghouse Engineer*, volume 3, August 1943, pages 76–9.

18. RELAY PROTECTION OF TAPPED TRANSMISSION LINES, M. A. Bostwick, E. L. Harder. AIEE TRANSACTIONS, volume 62, 1943, October section, pages 645–50.

Section C. Line Protection—Ground Faults

1941

1. ANALYZE UNUSUAL GROUND-RELAY PROBLEM, R. J. W. Koopman. *Electrical World*, volume 115, May 17, 1941, page 1667.

2. EARTH FAULT PROTECTION: WEAK SPOTS IN PRESENT-DAY METHODS, T. C. Gilbert. *Electrical Review* (London, England), volume 128, May 1941.

3. SENSITIVE GROUND PROTECTION FOR TRANSMISSION LINES AND DISTRIBUTION FEEDERS, E. T. B. Gross. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 968–71.

4. VOLTAGE-OPERATED EARTH-LEAKAGE PROTECTION, T. C. Gilbert. Institution of Electrical Engineers *Journal* (London, England), volume 88, June 1941, pages 183–97. Discussion, June 1941, pages 197–221, August 1941, pages 365–70.

1942

5. EARTH LEAKAGE PROTECTION, W. Gray. *Electrical Times* (London, England), volume 101, January 8, 1942, pages 421–2.

6. RELAYS AND BREAKERS FOR HIGH-SPEED SINGLE-POLE TRIPPING AND RECLOSING, S. L. Goldsborough, A. W. Hill. AIEE TRANSACTIONS, volume 61, 1942, February section, pages 77–81. Discussion, page 429.

1943

7. FILTER BANS 25-CYCLE FROM GROUND RELAY, R. B. Squires. *Electrical World*, volume 119, June 26, 1943, page 88.

Section D. Line Protection—General

1940

1. APPLICATIONS AND LIMITATIONS OF INVERSE-TIME OVERCURRENT RELAY TO PROTECTION OF 11-KV NETWORK, R. H. Bousfield, J. W. Gallop. Institution of Electrical Engineers *Journal* (London, England), volume 87, August 1940, pages 113–29; discussion, pages 129–34.

2. BIASED DIFFERENTIAL RELAYS FOR FEEDER PROTECTION. *Engineering Journal*, volume 149, February 2, 1940, pages 125–6.

3. PROTECTION OF HIGH-VOLTAGE TRANSMISSION SYSTEMS, P. Sporn. *Electrical News and Engineering* (Toronto, Ont., Canada), volume 49, June 15, 1940, pages 104–06, 111; July 15, 1940, pages 23–5.

1941

4. AN IMPROVED POLYPHASE DIRECTIONAL RELAY, B. V. Hoard. AIEE TRANSACTIONS, volume 60, 1941, May section, pages 246–8. Discussion, page 633.

5. CONTROLLED TIME DELAY PREVENTS FAULTY TRIPPING OF CIRCUIT BREAKERS, G. D. Gamel. *Power Plant Engineering*, volume 45, September 1941, page 78.

6. FUNDAMENTALS OF CO-ORDINATING FUSES AND RELAYS, R. M. Wilson, C. E. Cannon. *Electrical West*, volume 87, July 1941, pages 30–1.

7. HOW TO GIVE PROTECTOR TUBES A CHANCE, S. L. Goldsborough. *Electrical World*, volume 115, June 14, 1941, page 2054.

8. Novel Scheme Co-ordinates Relays and Fused Taps, M. R. Howell. *Electrical World*, volume 116, August 9, 1941, page 430.

9. Setting of Overload Relays: Effect on Current-Transformer Burden, P. S. Roy. *Electrical Review* (London, England), volume 128, April 1941, page 540.

10. Some Features of Steel-Plant Power-Transmission Protection, F. O. Schnure. *Iron and Steel Engineer*, volume 18, November 1941, pages 30–2.

1942

11. High-Speed Relaying, W. A. Lewis. *Proceedings* of the Midwest Power Conference, volume 5, 1942, pages 137–55.

12. How to Connect Controls, Meters, and Relays for 60- and 100-Kv Transmission Lines. *Electrical World*, volume 118, July 25, 1942, page 322.

13. How to Connect Controls, Meters, and Relays for Parallel 200-Kv Transmission Lines. *Electrical World*, volume 118, December 12, 1942, page 1980.

1943

14. A New High-Speed Balanced-Current Relay, V. N. Stewart. AIEE Transactions, volume 62, 1943, August section, pages 553–5.

15. A New One-Cycle Directional Overcurrent Relay, W. C. Morris. AIEE Transactions, volume 62, 1943, November section, pages 725–7.

16. Directional Overcurrent Relay, W. C. Morris. *General Electric Review*, volume 46, October 1943, pages 571–4.

17. The Principles of High-Speed Relaying, W. A. Lewis. *Westinghouse Engineer*, volume 3, August 1943, pages 91–4; November 1943, pages 131–4.

Section E. Bus Protection

1941

1. Bus Protection Independent of Current-Transformer Characteristics, G. Steeb. AIEE Transactions, volume 60, 1941, September section, pages 859–62.

2. Quadruplex Checks Differential Relays, J. H. Kinghorn. *Electrical World*, volume 116, November 29, 1941, page 1752.

3. Relay Protects Bus With Feeder Reactors, W. K. Sonnemann. *Electrical World*, volume 115, January 25, 1941, page 342.

1942

4. A New Single-Phase-to-Ground Fault-Detecting Relay, W. K. Sonnemann. AIEE Transactions, volume 61, 1942, September section, pages 677–80. Discussion, page 995.

5. Air-Core Couplers Simplify Differential Protection, E. L. Harder. *Westinghouse Engineer*, volume 2, May 1942, pages 43–5.

6. Bus-Bar Protection: A Critical Review of Methods and Practice, M. Kaufmann, W. Szwander. Institution of Electrical Engineers *Journal* (London, England), November 1942.

7. Fault-Bus Differential Scheme Protects Cahokia Bus, G. W. Gerell. *Electrical World*, volume 117, April 18, 1942, pages 1335–6.

8. Linear Couplers for Bus Protection, E. L. Harder, E. H. Klemmer, W. K. Sonnemann, E. C. Wentz. AIEE Transactions, volume 61, 1942, May section, pages 241–8. Discussion, page 463.

9. Predicting Performance of Bus-Differential Systems, E. C. Wentz, W. K. Sonnemann. *Westinghouse Engineer*, volume 2, February 1942, pages 28–31.

1943

10. Damage Local and Less With Fault-Bus Relaying, I. C. Eppley. *Electrical World*, volume 120, August 21, 1943, pages 633–4.

11. Linear Couplers Used for Bus Protection, W. K. Sonnemann. *Electrical World*, volume 120, July 10, 1943, page 86.

12. The Effect of Current-Transformer Residual Magnetism on Balanced-Current or Differential Relays, H. T. Seely. AIEE Transactions, volume 62, 1943, April section, pages 164–8. Discussion, page 384.

Section F. Apparatus Protection

1940

1. A High-Speed Differential Relay for Generator Protection, W. K. Sonnemann. AIEE Transactions, volume 59, 1940, November section, pages 608–12. Discussion, page 1250.

2. Protecting Motors in the Steel Mill, G. A. Caldwell. *Steel*, volume 106, April 8, 1940, pages 60–2.

3. Relay and Circuit-Breaker Protection for D-C Machines, H. Bany. *General Electric Review*, volume 43, August 1940, pages 312–19.

1941

4. Centrifugal Relay Protects Turbine. *Electrical World*, volume 115, June 1941, page 2226.

5. D-C Machine Flashover and Bus Short-Circuit Protection, T. B. Montgomery, J. F. Sellers. AIEE Transactions, volume 60, 1941, pages 1168–73; discussion, page 1372.

6. Harmonic-Current-Restraint Relays for Transformer Differential Protection, C. D. Hayward. AIEE Transactions, volume 60, 1941, pages 377–82; discussion, page 622.

7. Prolonged Inrush Currents With Parallel Transformers Affect Differential Relaying, C. D. Hayward. AIEE Transactions, volume 60, 1941, December section, pages 1096–1101. Discussion, page 1305.

1942

8. A Practical Discussion of Problems in Transformer Differential Protection, P. W. Shill. AIEE Transactions, volume 61, 1942, December section, pages 854–8. Discussion, page 1067.

9. Added Protection for Synchronous Condensers. *Electrical World*, volume 117, June 27, 1942, page 2238.

10. How to Connect Controls, Meters, and Relays for A-C Generators. *Electrical World*, volume 117, May 16, 1942, page 1698.

11. How to Connect Controls, Meters, and Relays for Station Service Transformer Banks. *Electrical World*, volume 117, May 2, 1942, page 1541.

12. How to Connect Controls, Meters, and Relays for Synchronous Condensers. *Electrical World*, volume 118, October 31, 1942, page 1480.

13. How to Connect Controls, Meters, and Relays for Three-Winding Transformer Bank. *Electrical World*, volume 118, September 19, 1942, page 966.

14. Loss-of-Field Protection for Generators, G. C. Crossman, H. F. Lindemuth, R. L. Webb. AIEE Transactions, volume 61, 1942, May section, pages 261–6. Discussion, page 462.

1943

15. A-C Generator: Protective Equipment in Continental Practice, W. Szwander. *Electrician* (London, England), volume 130, May 28, 1943, pages 543–6.

16. A New Generator Differential Relay, A. J. McConnell. AIEE Transactions, volume 62, 1943, January section, pages 11–13. Discussion, page 381.

17. Carrier-Current Differential Protection for Transformer Banks, T. A. Cramer. AIEE Transactions, volume 62, 1943, August section, pages 545–8.

18. Neutral Relays Guard Reactors, Generator, H. E. Chambers. *Electrical World*, volume 119, February 20, 1943, page 86.

Section G. Distribution and Network Protection

1940

1. Biased Differential Relays for Feeder Protection. *Engineering* (London, England), volume 149, February 2, 1940, pages 125–6.

2. Sensitive Ground Protection for Radial Feeders, L. F. Hunt, J. H. Vivian. AIEE Transactions, volume 59, 1940, February section, pages 84–7; discussion, page 87.

1941

3. Design Features of a New-High-Capacity Network Protector, J. W. Seaman, R. W. Smith. *General Electric Review*, volume 44, December 1941, page 673.

4. Modern Relaying for A-C Secondary-Network Systems, D. L. Beeman. AIEE Transactions, volume 60, 1941, May section, pages 237–41. Discussion, page 683.

5. Network-Protector Operations Reduced, W. E. Edson, M. A. Bostwick. *Electrical World*, volume 115, February 22, 1941, page 56.

6. Overload Protection of Power Distribution System Prevents Costly Delays in Conveyor Mining, H. P. Chandler. *Coal Age*, volume 46, page 57.

7. Short-Circuit Conditions on Large Industrial Low-Voltage Distribution Networks, T. S. G. Seaword. Institution of Electrical Engineers *Journal* (London, England), volume 88, March 1941.

1942

8. High-Tension Distribution, W. A. Mears. *Electrician* (London, England), volume 128, April 10, 1942, pages 337–43.

9. Performance of Ground-Relayed Distribution Circuits During Faults to Ground, C. L. Gilkeson; P. A. Jeanne; J. C. Davenport, Jr. AIEE Transactions, volume 61, 1942, January section, pages 40–8. Discussion, page 424.

10. Protective Systems in 11-Kv Distribution, C. Freeland. *Electrician* (London, England), volume 128, April 19, 1942, pages 334–5.

11. Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits, G. F. Lincks, C. R. Craig. AIEE Transactions, volume 61, 1942, November section, pages 813–20. Discussion, page 980.

Section H. Service Restoration

1940

1. Inertia Relay "Anticipates" to Facilitate Reclosure, J. T. Logan, J. H. Miles. *Electrical World*, volume 113, April 1940, pages 1061–4.

1941

2. A Novel Reclosing Relay, P. O. Bobo. AIEE Transactions, volume 60, 1941, December section, pages 1041–5. Discussion, page 1403.

3. An Automatically Reclosing Breaker for Co-ordinating Distribution Systems, H. L. Rawlins, J. M. Wallace. AIEE Transactions, volume 60, 1941, December section, pages 1021–4.

4. Factors Contributing to Improving Electric Service by Means of High-Speed Switching and Utilization of Stored Energy, J. H. Miles, J. T. Logan. AIEE Transactions, volume 60, 1941, December section, pages 1012–16. Discussion, page 1407.

5. Five Year's Experience With Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines, Philip Sporn, C. A. Muller. AIEE Transactions, volume 60, 1941, May section, pages 241–6. Discussion, page 690.

6. Reclosing Breakers or Reclosing Fuses—Which? H. A. P. Langstaff. *Electrical World*, volume 115, March 22, 1941, page 974.

7. Single-Phase Switching Applied to 132 Kv, J. J. Trainor. *Electrical World*, volume 115, May 17, 1941, page 1667.

8. Transfer Airport Load Among Three Circuits. *Electrical World*, volume 116, November 1, 1941, page 1444.

1942

9. A New Single-Pole Service Restorer, E. E. Tugby. AIEE Transactions, volume 61, 1942, December section, pages 889–92. Discussion, page 1070.

10. Analysis of the Application of High-Speed Reclosing Breakers to Transmission Systems, S. B. Crary, L. F. Kennedy, C. A. Woodrow. AIEE Transactions, volume 61, 1942, June section, pages 339–48. Discussion, page 423. Also see *General Electric Review*, volume 45, March 1942, pages 173–85.

11. Analyzer Guides Adjustment of Ultra-high Speed Breakers, E. L. Schlottere, R. M. Seitz, S. Goldsmith. *Electrical World*, volume 117, February 21, 1942, pages 652–3.

12. Automatic Circuit-Breaker Reclosing, A. C. Schwager. *Electrical West*, volume 88, June 1942, pages 95–8.

13. Field Tests and Performance of High-Speed 138-Kv Air-Blast Circuit Breaker, Philip Sporn, H. E. Strang. AIEE Transactions, volume 61, 1942, January section, pages 1–6. Discussion, page 412.

14. High-Speed Single-Pole Reclosing, J. J. Trainor; J. E. Hobson; H. N. Muller, Jr. AIEE Transactions, volume 61, 1942, February section, pages 81–7. Discussion, page 442.

15. Throw-Over and Reclosure in Simplified Combination, A. E. Anderson. *Electrical World*, volume 117, May 2, 1942, pages 1503–4.

1943

16. Interim Report on Application and Operation of Automatic Reclosing Equipment on Stub Feeders, AIEE committee on automatic stations. AIEE Transactions, volume 62, 1943, August section, pages 536–40.

17. One Relay Recloses Several Breakers, H. A. P. Langstaff. *Electrical World*, volume 120, October 16, 1943, page 102.

Section I. General and Miscellaneous Relaying

1940

1. ABC's of Relays, E. B. Kipp. *Electrical West*, volume 85, August 1940, pages 44–5; September 1940, pages 42–3; October 1940, pages 52–3; November 1940, page 47; December 1940, pages 47–8.

2. A Condensation of the Theory of Relays, A. C. van C. Warrington. *General Electric Review*, volume 43, September 1940, pages 370–3.

3. Capacitor Relay Timing in Industrial Control, T. B. Jochem, C. Stansbury. AIEE Transactions, volume 59, 1940, February section, pages 65–9; discussion, page 69.

4. Modern Protective Relays and Relaying Systems, Taniani-Seiiti. *Shibaura Review* (Yokohama, Japan), volume 19, May 1940, pages 185–94; June 1940, pages 237–48; August 1940, pages 309–20; September 1940, pages 359–70.

5. Rack Co-ordinates Relay Selectivity, C. L. Betts. *Electrical World*, volume 114, October 1940, pages 996–8, 1058.

6. Relay Scheme Stops Synchronizing Errors, E. F. Miller. *Electrical World*, volume 113, January 1940, pages 314–16.

7. Switchboard Designed to Illustrate Relay Performance, A. J. Cowan. *General Electric Review*, volume 43, September 1940, pages 402–05.

1941

8. ABC's of Relays, E. B. Kipp. *Electrical West*, volume 86, January 1941, pages 36–8; March 1941, pages 45–6.

9. Auxiliary Relays in Old Installations, M. R. Howell. *Electrical World*, volume 115, January 11, 1941, page 157.

10. Drawout Relays, J. A. Favre, J. H. Oliver. *General Electric Review*, volume 44, April 1941, page 223.

11. Electric Overvoltage Relays, G. G. Kretschmar. *Electronics*, volume 14, February 1941, page 48.

12. Electric Relays: Constructional Details and Notes on the Use of Magnetic Mercury, Thermionic and Photoelectric Relays (book), J. Rosslyn. Chemical Publishing Company, Inc., Brooklyn, N. Y., 1941. 184 pages.

13. Electronic Integrator for Counting Circuit Contacts, G. W. Kenrick. *Electronics*, volume 14, March 1941, pages 33–5.

14. Electronic Relay With Improved Characteristics, R. C. Howes. *Science*, volume 93, January 1941, page 24.

15. New Fields in Magnetic Contact Relays, A. H. Lamb. *Electronics*, volume 14, February 1941, page 31.

16. Premiums From Protective Relaying, C. R. Mason. *General Electric Review*, volume 44, March 1941, page 181.

17. Protective Relaying for Attended Power Plants, C. R. Mason. *Power Plant Engineering*, volume 45, December 1941, pages 77–9.

18. Relay Causes Delay of From Five to Forty Cycles, C. T. Altfather. *Electrical World*, volume 115, March 22, 1941, page 1010.

19. Sun-Spot Disturbance of Terrestrial Magnetism, W. F. Davidson. Electrical Engineering, volume 60, February 1941, pages 72–5.

20. Supersensitive Relays for Precision Performance. *Electrical Manufacturing*, volume 28, July 1941, pages 49–52, 77, 98.

21. Supplementary Relays: Devices Employed in Modern Protective Schemes, C. L. Lipman. *Electrical Review* (London, England), volume 128, February 1941, page 410.

22. System Protective Analysis Precedes Oswego Design, G. Steeb. *Electrical World*, volume 116, November 1, 1941, page 1413.

23. Ten Years' Progress in Relaying, AIEE protective devices committee. Electrical Engineering, volume 60, December 1941, pages 590–2.

24. Vacuum-Tube Time-Delay Relay, E. F. Serfass. *Industrial and Engineering Chemistry*, analytical edition, volume 13, May 1941, page 352.

25. When Specifying Relays, R. Vradenburgh. *Electrical Manufacturing*, volume 28, September 1941, pages 44–7, 86, 88, 90.

1942

26. Electrical Timing Devices: Diagrams. *Product Engineer*, volume 13, January 1942, 26–7.

27. Factors Which Influence the Behavior of Directional Relays, T. D. Graybeal. AIEE Transactions, volume 61, 1942, pages 942–52.

28. How to Determine Relaying Quantities by the Use of Sequence Segregating Filters or Networks. *Electrical World*, volume 117, June 13, 1942, page 2088.

29. Londex Time-Delay Relay. *Electrician* (London, England), volume 128, March 6, 1942, page 193.

30. Management of Protective Gear on Power Systems, W. Casson, F. H. Birch. *Journal* Institution of Electrical Engineers (London, England), volume 89, part 2, August 1942, pages 317–41; discussion, pages 341–54.

31. Progress in Relaying, P. MacGahan. Electrical Engineering, volume 61, February 1942, page 106.

32. Relay and Circuit-Breaker Application, E. L. Harder, J. C. Cunningham. Electrical Transmission and Distribution Reference Book, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., 1942. Pages 229–72.

33. Relays Classified by Types to Simplify Their Selection, R. B. Immel. *Product Engineer*, volume 13, October and November 1942, pages 591–6, 647–52.

34. Relay Contacts: New Material for Heavy-Duty Powder-Metallurgy Product, Silver–Nickel Elkonite, H. R. Brooker. *Electrical Review*, volume 130, May 22, 1942, pages 651–2.

35. Relaying Protects Power-Station Equipment, S. C. Leyland. *Electric Light and Power*, volume 20, March 1942, pages 58–61.

36. Relay Inverse-Time Characteristics Doubled, G. Steeb. *Electrical World*, volume 118, October 31, 1942, page 1484.

37. Sectional Relay Panel Permits Regrouping, J. C. Woods. *Electrical World*, volume 117, March 7, 1942, pages 842–3.

38. Simple Photoelectric Relay, E. B. Working. *Science*, volume 96, September 18, 1942, pages 281–2.

39. Some Canadian Developments in Relays and Relay Applications, E. G. Ratz. *Electrical News* (Toronto, Ont., Canada), volume 51, November 15, 1942, pages 31–3; December 1, 1942, pages 20–2.

40. Static Relay, W. R. Thistlewaite. *Electrical Review* (London, England), volume 130, February 6, 1942, pages 167–8.

41. Temperature Co-ordination of Relays and Fuses, M. R. Howell. *Electrical World*, volume 118, July 11, 1942, pages 72–4.

42. Time-Delay Relay, H. Seymour. *Electrician* (London, England), volume 129, August 7, 1942, pages 142–3.

43. Time-Delay Relays and Their Characteristics, R. B. Immel. *Product Engineer*, volume 13, February 1942, pages 86–8.

44. Time Relay Extends Breaker Life. *Electrical World*, volume 118, July 25, 1942, page 326.

1943

45. Cold-Cathode Tubes Used on Relays, E. C. Schurch. *Electrical World*, volume 119, February 6, 1943, page 68; February 20, 1943, page 82; March 6, 1943, page 76; March 20, 1943, page 112.

46. Combine Relay Case and Test Switch, S. C. Leyland. *Electrical World*, volume 120, November 13, 1943, page 100.

47. Contact Bounce, P. H. Estes. *General Electric Review*, volume 46, June 1943, pages 321–3.

48. Improved Form of Sensitive Relay, V. Legallais. *Review of Scientific Instruments*, volume 14, February 1943, pages 51–2.

49. Instance of Corrosion in Electric Relays: Phosphor-Bronze Springs, R. Wood, G. H. Wyatt. *Journal* of the Society of Chemical Industry (London, England), volume 62, July 1943, pages 110–11.

50. Line Relayed to Adjust Local Stations to Local Loads, J. F. Pennington, J. T. Logan. *Electrical World*, volume 119, pages 71–3.

51. Protective Relays: Notes on the New British Standards Specifications, W. Casson. *Electrical Review* (London, England), volume 132, March 12, 1943, pages 346–8.

52. Relaying Lines Added for "Big Inch" Pumps, H. A. P. Langstaff. *Electrical World*, volume 120, September 18, 1943, pages 108–10.

53. Temperature Relay Cuts Power Load, A. G. Johnson. *Electrical World*, volume 119, February 20, 1943, page 620.

54. Thyratron Thermal Relay, D. Rittenberg, I. Sucher. *Review of Scientific Instruments*, volume 14, February 1943, page 51.

Section J. Testing and Analyzing

1940

1. New A-C and D-C Testing Equipment on Hudson and Manhattan. *Railway Signaling*, volume 33, November 1940, page 461.

2. Maintenance of Relays and Associated Equipment, J. P. Brookman. Institution of Electrical Engineers *Journal* (London, England), volume 87, November 1940, pages 485–94; discussion, pages 494–506.

1941

3. Analysis of Short-Circuit Oscillograms, W. W. Kuyper. AIEE Transactions, volume 60,

1941, April section, pages 151–3. Discussion, page 752.

4. Circuit for Testing Current Transformers, E. A. Walker. *Review of Scientific Instruments*, volume 12, February 1941, pages 85–6.

5. High-Speed Condenser Timer, E. T. Vickerman, Jr.; S. E. Edlestein. *Electrical West*, volume 86, June 1941, pages 141–6.

6. Improved Time-Interval Meter for Testing Protective Gear Relays, R. W. Langley. *Electrician* (London, England), volume 127, October 1941, pages 249–50.

7. Instrument Assembly Simplifies Relay Field Testing, A. J. McCahan. *Electrical World*, volume 116, July 26, 1941, page 213.

8. Instrumentation: Chief Trends and Some New Data, M. F. Behar. *Metal Progress*, volume 40, October 1941, page 629.

9. Network Analyzer Solves System Problems, G. W. Bills. *Electrical West*, volume 87, December 1941.

10. Transient Analyzer Checks Breaker and Bus Protection, J. H. Neher, R. J. Woodrow. *Electrical World*, volume 116, December 13, 1941, page 1885.

11. Transmission-System Faults: Use of Measurements of Fault Conditions, W. T. J. Atkins. *Electrical Review* (London, England), volume 128, January 1941, page 253.

1942

12. High-Speed Condenser Timer for Testing, E. T. Vickerman, Jr.; S. E. Edlestein. *Electrical World*, volume 117, May 2, 1942, page 1534.

13. How to get Clearer Oscillograph Films. *Electrical World*, volume 117, June 27, 1942, page 2250.

14. Improved Design for Relay Test Set, B. J. Bortell, J. E. Koslowsky. *Electrical World*, volume 118, December 1942, pages 2094–5.

15. Low-Burden Ammeter for Relay Testing, H. L. Goodridge. *Electrical World*, volume 118, July 25, 1942, page 318.

16. Oscillograph Adapted for Retracing Improves Records, R. W. Ahlquist. *Electrical World*, volume 117, January 10, 1942, page 100.

17. Oscillograph Scale Printed on Form, E. J. Mommo. *Electrical World*, volume 117, March 7, 1942, page 833.

18. Oscillograph Starts on All Faults, V. D. Hollis. *Electrical World*, volume 118, September 5, 1942, pages 806–07.

1943

19. An Instrument for the Determination of Contact Making and Breaking Time, Walter Richter, William H. Elliot. AIEE Transactions, volume 62, 1943, January section, pages 14–16.

20. Marked Oscillograph Tracings, R. W. Ahlquist. *General Electric Review*, volume 46, March 1943, pages 179–82.

21. Relay and Switch Upkeep and Trouble Shooting: Data Sheets, W. A. Holland. *Factory Management*, volume 101, February 1943, page 134.

22. Relay-Testing Practice by Wisconsin Utilities. *Electrical World*, volume 119, May 1, 1943, page 1486.

23. The Latest in Magnetic Oscillographs, K. R. Geiser, J. E. Hancock. *General Electric Review*, volume 46, May 1943, pages 289–94.

24. Typewriting on Film Records Saves Time, W. C. Curl. *Electrical World*, volume 120, July 24, 1943, page 123.

Section K. System Stability

1940

1. Graphical Solution of Transient Stability, H. H. Skilling, M. H. Yamakawa. Electrical Engineering, volume 59, November 1940, pages 462–5.

Power-System Interconnections (book), H. Rissik. Pitman Publishers, 1940.

1941

2. Limitations Placed on Power Transmission by System Stability. *Electrical News*, volume 50, June 1941, pages 22–5.

3. Problems of Transient Stability in Interconnected Transmission Systems, H. Rissik. *Electrician* (London, England), volume 126, April 1941, page 243.

4. Stability Limitations of Long-Distance A-C Power-Transmission Systems, Edith Clarke, S. B. Crary. AIEE Transactions, volume 60, 1941, December section, pages 1051–9. Discussion, page 1299.

5. System Load Swings, H. A. Bauman; O. W. Manz, Jr.; J. E. McCormack; H. B. Seeley. AIEE Transactions, volume 60, 1941, pages 541–7; discussion, page 735.

6. System Stability, F. W. Gay. AIEE Transactions, volume 60, 1941, April section, pages 168–73. Discussion, page 1351.

7. Turboalternator Speed Control: Improving Stability on an Interconnected System, A. Abbott. *Electrical Review* (London, England), volume 128, June 1941, page 713.

1942

8. Control and Regulation of Utility Industrial Ties, L. F. Stone. *Electrical World*, volume 117, April 4, 1942, pages 1171–3.

9. Control of Tie-Line Power Swings, C. Concordia, H. S. Shott, C. N. Weygandt. AIEE Transactions, volume 61, 1942, June section, pages 306–14. Discussion, page 395.

10. Correct Electrical Protection Makes Interchange Safe. *Power*, volume 86, September 1942, pages 630–2.

11. Equivalent Circuits for the Hunting of Electrical Machinery, G. Kron. AIEE Transactions, volume 61, 1942, May section, pages 290–6.

12. Power-System Stability—Basic Elements of Theory and Application, R. D. Evans. Electrical Transmission and Distribution Reference Book, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., 1942. Pages 175–210.

13. Single-Pole Fault Clearing for Greater Stability, J. E. Hobson; H. N. Muller, Jr. *Westinghouse Engineer*, volume 2, February 1942, pages 23–5.

14. Sequence Relay: A Device to Provide Short-Time Separation on Long-Period Oscillations, C. L. Betts, Jr. Edison Electric Institute *Bulletin*, volume 10, August 1942, pages 301–09.

15. Stability Study of A-C Power-Transmission Systems, J. G. Bolm. AIEE Transactions, 1942, pages 893–905; discussion, page 1046.

16. Stability of Oscillations in Systems Obeying Mathiens Equation, J. G. Brainerd. Franklin Institute *Journal*, volume 233, February 1942, pages 135–42.

17. System Stability—Examples of Calculation, H. N. Muller, Jr. Electrical Transmission and Distribution Reference Book, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., 1942. Pages 211–28.

18. Three-Winding-Transformer Ring-Bus Characteristics, G. W. Bills, C. A. MacArthur. AIEE Transactions, volume 61, 1942, December section, pages 848–9. Discussion, page 1066.

19. Time-Sequence Relay for System Separation, C. L. Betts, Jr. *Electrical World*, volume 118, December 26, 1942, page 2096.

1943

20. Equivalent Circuits for Oscillating Systems and Riemann-Christoffel Curvature Tensor, G. Kron. AIEE Transactions, volume 62, 1943, January section, pages 25–31. Discussion, page 372.

21. Interim Report on Application and Operation of Out-of-Step Protection, AIEE relay subcommittee. AIEE Transactions, volume 62, 1943, September section, pages 567–73.

22. Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems, R. B. Bodine, C. Concordia, G. Kron. AIEE Transactions, volume 62, 1943, January section, pages 41–4. Discussion, page 371.

23. Staged Fault Tests of Relaying and Stability on Kansas–Nebraska 270-Mile 154-Kv Interconnection, C. W. Minard, R. B. Gow, W. A. Wolfe, E. A. Swanson. AIEE Transactions, volume 62, 1943, pages 358–67.

24. Steady-State and Hunting Equivalent Circuits of Long-Distance Transmission Systems, G. Kron. *General Electric Review*, volume 46, June 1943, pages 337–42.

25. Steady-State and Transient-Stability Analysis of Series Capacitors in Long Transmission Systems, J. W. Butler, J. E. Paul, T. W. Schroeder. AIEE Transactions, volume 62, 1943, February section, pages 58–65. Discussion, page 377.

26. Transmission-System Stability Problems Grow With Loads, F. Von Voigtlander. *Electric Light and Power*, volume 21, September 1943, pages 54–6.

Section L. Methods of Calculation

1940

1. An Extension of the Method of Symmetrical Components Using Ladder Networks, W. V. Lyon. AIEE Transactions, volume 59, 1940, pages 1025–30; discussion, page 1263.

2. Determination of Short-Circuit Currents in Low-Voltage Industrial Feeders, E. F. Kubler. *General Electric Review*, volume 43, September 1940, pages 459–66.

3. Transformation Theory of General Static Polyphase Networks, L. A. Pipes. AIEE Transactions, volume 59, 1940, February section, pages 123–8; discussion, page 128.

4. Transient Analysis of Symmetrical Networks by the Method of Symmetrical Components, L. A. Pipes. AIEE Transactions, volume 59, 1940, August section, pages 457–9. Discussion, page 1107.

1941

5. A-C Vectors: Significance of the Operator, A. G. Powell. *Electrical Review* (London, England), volume 129, December 19, 1941, page 694.

6. Calculation of Fault Currents in Industrial Plants, Raymond C. R. Schulze. Electrical Engineering, volume 60, June 1940, pages 271–9.

7. Fault Voltage Drop and Impedance at Short-Circuit Currents in Low-Voltage Circuits, O. R. Schurig. AIEE Transactions, volume 60, 1941, pages 479–87; discussion, page 654.

8. Ground Resistance—Extensive Study of Northwest Soils and Transmission Tower Grounding, H. V. Strandberg. *Electrical West*, volume 86, January 1941.

9. Reactance Formulas for Stranded Steel, T. J. Higgins. *Electrical World*, volume 116, July 1941, page 296.

10. Short-Circuit Calculating Procedure for Low-Voltage A-C Systems, A. C. Darling. AIEE Transactions, volume 60, 1941, pages 1121–36; discussion, page 1295.

11. Steady-State Analysis of Multiconductor Transmission Lines, L. A. Pipes. *Journal of Applied Physics*, volume 12, November 1941, page 782.

12. Symmetrical-Component Currents: A Two-Wattmeter Method of Measurement, G. W. Stubbings. *Electrician* (London, England), volume 127, October 31, 1941, pages 255–6.

13. System Short-Circuit Currents, W. M. Hanna, H. A. Travers, C. F. Wagner, C. A. Woodrow, W. F. Skeats. AIEE Transactions, volume 60, 1941, September section, pages 877–81. Discussion, page 1351.

14. The Modern A-C Network Calculator, W. W. Parker. AIEE Transactions, volume 60, 1941, November section, pages 977–82. Discussion, page 1395.

15. Transient Analysis of Completely Transposed Multiconductor Transmission Lines, L.

A. Pipes. AIEE Transactions, volume 60, 1941, pages 346–51; discussion, page 621.

16. Zero-Sequence Impedances of Overhead Three-Phase and Single-Phase Transmission Circuits, Edith Clarke, G. K. Carter. *General Electric Review*, volume 44, June 1941, page 329.

1942

17. A-C Calculation Charts (book), R. Lorenzen. John F. Rider, Publisher, Inc., New York, N. Y., 1942.

18. Analysis of Short Circuits for Distribution Systems, C. F. Dalziel. AIEE Transactions, volume 61, 1942, October section, pages 757–64. Discussion, page 1048.

19. Fault-Current Calculation, W. A. Mears. *Electrician* (London, England), volume 129, July 10–24, 1942, pages 33–7, 58–60, 84–7.

20. Quicker Way of Computing Distribution Short Circuits. J. D. Sauter, G. F. Lincks. *Electrical World*, volume 117, March 21, 1942, pages 992–3.

21. Simplified Calculation of Fault Currents, AIEE committee on protective devices. Electrical Engineering, volume 61, October 1942, pages 509–11.

22. Study of Driven Rods and Counterpoise Wires in High-Resistance Soil on Consumer's Power 140-Kv System, J. G. Hemstreet, W. W. Lewis, C. M. Faust. AIEE Transactions, volume 61, 1942, September section, pages 628–33.

23. Symmetrical Components, J. E. Hobson. Electrical Transmission and Distribution Reference Book, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., 1942, pages 11–28.

24. Symmetrical Components, Analysis by Vector Notation, A. G. Powell. *Electrical Review* (London, England), volume 131, July 24, 1942, pages 107–08.

25. Symmetrical Components: Unbalanced Three-Phase Problems Simplified. *Electrical Review* (London, England), volume 130, January 9, 1942, pages 45–6.

26. System Short Circuits: Calculation of the Currents When Applying Interrupting Devices and Relays, C. A. Woodrow. *General Electric Review*, volume 45, June 1942, pages 357–9.

27. Zero-Sequence Voltages, G. W. Stubbings. *Electrician* (London, England), volume 129, July 31, 1942, pages 110–12.

1943

28. A Useful Equivalent Circuit for a Five-Winding Transformer, L. C. Aicher, Jr. AIEE Transactions, volume 62, 1943, February section, pages 66–70. Discussion, page 385.

29. Circuit Analysis of A-C Power Systems (book), Edith Clarke. John Wiley and Sons, New York, N. Y., 1943.

30. Circuit Calculations: Examples of Graphical Methods, G. W. Stubbings. *Electrical Review* (London, England), volume 132, April 2, 1943, pages 449–50.

31. How to Determine Short-Circuit Current and Interrupting Capacity of Interrupting Devices, W. A. Holland. *General Electric Review*, volume 46, September 1943, pages 499–500.

32. Ground Faults Solved Easily With D-C Board. *Electric Light and Power*, volume 21, April 1943, pages 66–70.

33. Short-Circuit Analysis: Faults Between Two Phases, T. F. Wall. *Electrical Review* (London, England), volume 133, July 9, 1943, pages 43–4.

34. Short-Circuit Calculations. *Electrical Review* (London, England), volume 132, January 15, 1943, page 72.

35. Short-Circuit Currents (On Low-Voltage Systems), A. E. Anderson. *General Electric Review*, volume 46, June 1943, pages 316–19; July 1943, pages 399–401.

36. Short-Circuit Values: An Application of Symmetrical-Component Analysis, T. F. Wall. *Electrical Review* (London, England), volume 132, February 26, 1943, pages 281–2.

37. Symmetrical Components, G. W. Stubbings. *Electrician* (London, England), volume 130, January 8, 1943, pages 34–5.

38. Symmetrical-Component Analysis: Vectorial Treatment for Standard Short-Circuit Solutions, R. N. Buttrey. *Electrical Review* (London, England), volume 132, January 22, 1943, pages 113–15.

39. Symmetrical-Component Currents, G. W. Stubbings. *Electrician* (London, England), volume 131, July 16, 1943, pages 61–2.

40. The Effects of Mutual Induction Between Parallel Transmission Lines on Current Flow to Ground Faults, J. I. Holbeck, Martin J. Lantz. AIEE Transactions, volume 62, 1943, November section, pages 712–15.

Section M. Instrument Transformers and Other Auxiliary Devices

1940

1. A Proposed Method for the Determination of Current-Transformer Errors, G. Camilli, R. L. TenBroeck. AIEE Transactions, volume 59, 1940, September section, pages 547–50. Discussion, page 1138.

2. Analysis of Bushing-Current-Transformer Performance, J. A. Elzi, J. P. Gebelein. AIEE Transactions, volume 59, 1940, September section, pages 541–6. Discussion, page 1137.

3. Computation of Accuracy of Current Transformers, A. T. Sinks. AIEE Transactions, volume 59, 1940, December section, pages 663–8. Discussion, page 1352.

4. Current Transformers and Relays for High-Speed Differential Protection, With Particular Reference to Offset Transient Currents, W. K. Sonnemann, E. C. Wentz. AIEE Transactions, volume 59, 1940, August section, pages 481–8. Discussion, page 1144.

5. New Developments in Current-Transformer Design, G. Camilli. AIEE Transactions, volume 59, 1940, pages 835–42; discussion page 1099.

6. Overcurrent Performance of Bushing-Type Current Transformers, S. A. Bottonari; C. A. Woods, Jr. AIEE Transactions, volume 59, 1940, September section, pages 554–60. Discussion page 1140.

7. Potential Transformers Tested in Service, W. C. Wagner. *Electrical World*, volume 114, November 1940, pages 48–9.

8. Rating of Potential Devices and Suggested Material for a Standard, J. E. Clem, P. O. Langguth. AIEE Transactions, volume 59, 1940, December section, pages 676–80. Discussion, page 1217.

9. Transient and Steady-State Performance of Potential Devices, E. L. Harder; P. O. Langguth; C. A. Woods, Jr. AIEE Transactions, volume 59, 1940, February section, pages 91–9; discussion, page 99.

1941

10. A Simple Method for Determination of Ratio Error and Phase Angle in Current Transformers, E. C. Wentz. AIEE Transactions, volume 60, 1941, October section, pages 949–54. Discussion, page 1369.

11. Criteria for Neutral Stability of Wye-Grounded-Primary Broken-Delta-Secondary Transformer Circuits, H. S. Shott, H. A. Peterson. AIEE Transactions, volume 60, 1941, November section, pages 997–1002. Discussion, page 1394.

12. Current-Transformer Errors: Watt-Hour Method of Measuring, G. W. Stubbings. *Electrician* (London, England), volume 126, January 1941, page 49.

13. How to Fuse Potential-Transformer Primary Circuits, C. L. Schuck. *General Electric Review*, volume 44, July 1941, page 385.

14. Neutralizing Transformers, Edison Electric Institute engineering report 44, EEI publication *H*-12, 1941, 16 pages.

15. New Current Transformers for Bus Differential Protection, L. F. Kennedy, A. T. Sinks. AIEE Transactions, volume 60, 1941, pages 1180–7; discussion, page 1363.

16. Unusual Instrument Transformer. *Electrical Review* (London, England), volume 128, May 1941, page 697.

1942

17. Current- and Potential-Transformer Standardization, AIEE committee on protective devices, current transformer subcommittee. AIEE Transactions, volume 61, 1942, September section, pages 698–706. Discussion, page 998.

18. Current-Transformer Characteristics for Relay Operation, C. E. Lamphier. *Electrical World*, volume 117, February 7, 1942, page 518.

19. Current-Transformer Errors, G. W. Stubbings. *Electrician* (London, England), volume 128, March 6, 1942, pages 189–90.

20. Current-Transformer Errors: Measurement by an Exciting Current Method, G. F. Freeman. *Electrical Review* (London, England), volume 131, July 10, 1942, pages 41–2.

21. Current-Transformer Performance Based on Admittance-Vector Locus, A. C. Schwager. AIEE Transactions, volume 61, 1942, January section, pages 26–30. Discussion, page 465.

22. Current Transformers: Short-Circuit Capacity of the Wound Type, H. E. Forrest. *Electrical Review* (London, England), volume 130, March 13, 1942, pages 335–7.

23. Transient Characteristics of Current Transformers During Faults, C. Concordia, C. N. Weygandt, H. S. Shott. AIEE Transactions, volume 61, 1942, May section, pages 280–5. Discussion, page 469.

24. Voltage Transformer Errors, G. W. Stubbings. *Electrician* (London, England), volume 129, July 17, 1942, pages 57–8.

1943

25. A Simple Method for the Determination of Bushing-Current-Transformer Characteristics, S. D. Moreton. AIEE Transactions, volume 62, 1943, September section, pages 581–5.

Corrections and Addenda to "Relay Bibliography, 1927–1939"

Section A. Line Protection—Distance

1935

21*a*. Construction Power for Fort Peck Dam, E. A. Childerhouse. *Electric Journal*, volume 32, February 1935, pages 47–53.

Section D. Line Protection—General

1930

1*a*. Modern Requirements for Protective Relays, O. C. Traver, L. F. Kennedy. AIEE Transactions, volume 49, 1930, page 1226.

Section E. Bus Protection

1937

5*a*. Relay Protection for Station Busses, W. A. Lewis, R. M. Smith. *Electric Journal*, volume 35, November 1937, page 451.

1938

6*a*. Harmonic-Current-Restrained Relays for Differential Protection, L. F. Kennedy, C. D. Hayward. AIEE Transactions, volume 57, 1938, May section, pages 262–71. (This entry appears in error under *1935*.)

Formulas for Calculating Short-Circuit Forces Between Channels Located Back to Back

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IN 1937 Arnold[1] published an analytic procedure enabling the designer of a single-phase two-conductor bus to determine combinations of conductor spacing and cross section that render the proximity effect zero—hence combinations that render the a-c resistance, and thus the eddy-current loss, a *minimum*. For explicit details the reader is referred to Arnold's paper. For our purpose it suffices to remark that in general the designer is afforded an indefinite number of combinations (of conductor spacing and cross section) from which to choose; that the exact cross sections determined by the theory are not simple in shape; that as the ratio of conductor spacing to perimeter of conductor cross section increases, the equivalent structural shape associated with a particular conductor spacing shifts from rectangular strap to channel to circular or square tubular conductor; that the a-c resistance, and hence the eddy-current loss, can be calculated from a given simple formula; and that the accuracy of this formula and the correctness of the general theory are verified by experimental data set forth in the paper.

Despite, however, the availability and the obvious usefulness of this general theory, it would seem that no considerable use, if any, has yet been made of it. In some part, no doubt, this neglect is due to lack of knowledge of it: Arnold's paper was published in the *Journal* of the Institution of Electrical Engineers, a British periodical which in this country enjoys but little circulation outside of engineering libraries or the technical departments of a few of the larger public libraries. In other part, however, this neglect doubtlessly can be attributed to the fact that, largely, a-c industrial-bus design is polyphase and has as its desideratum the achievement of minimum inductance, hence minimum reactance voltage drop. Designs achieving this end involve closely spaced conductors, thus greater than minimum eddy-current losses, whereas practical designs achieving minimum eddy-current losses involve fairly widely spaced conductors, thus increased reactance voltage drop.

In those instances, however, where single-phase busses are to be designed and minimum reactance is not so much the desideratum as is minimum eddy-current loss, Arnold's theory affords preferred designs. In consequence, it is of interest to the designer of such busses that there be available to him formulas enabling calculation of the electrical and mechanical performance of designs stemming from Arnold's theory. This, as already mentioned, yields conductor cross sections to be approximated by standard structural shapes: rectangular strap, channel, and circular or square tubular conductors.

Now formulas essential for calculating the a-c resistance, a-c inductance, mutual electromagnetic forces on short circuit, and other conductor parameters essential to knowledge of the electrical and mechanical (short-circuit) performance of the bus are available for rectangular strap and circular or square tubular conductors.[2] As regards (uniformly thick) channel conductors located back to back, Arnold has advanced a formula for calculating the a-c resistance and Higgins[3] has advanced a formula for calculating the mutual electromagnetic forces exerted between the conductors on short circuit. *But no formulas for the inductance have been published hitherto.* Yet, inasmuch as among the various combinations of conductor spacing and cross section afforded by Arnold's theory, that combination which affords the best all-around design is usually one comprised of moderately spaced channels placed back to back, it is most desirable to have formulas for the inductance. Such formulas have been derived by Schwantz and Higgins in a master-of-science thesis done by W. Schwantz under the supervision of T. J. Higgins, "Formulas for the Inductance of Busses Comprised of Channels Located Back to Back."

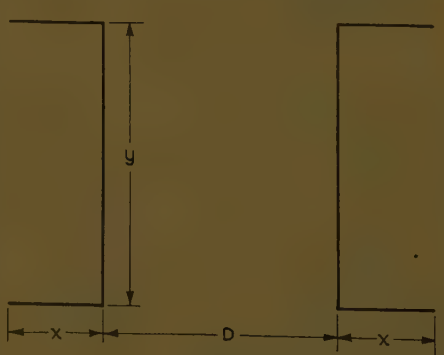


Figure 1

Subsequent to the derivation of and through the use of these formulas, the present writer has been able to derive new and improved formulas for calculating the mutual electromagnetic forces exerted on short circuit. These formulas—easier to use, more rapid in convergence, capable in general of greater accuracy than the earlier formula previously mentioned—are presented in this paper. Use of them considerably simplifies calculation of bus-bar support stresses.

The mechanical forces acting on bus-bar supports during short circuit are determined both by the magnitudes of the mutual electromagnetic forces exerted among the bus conductors and by the elastic properties of the supporting structure: its motional resistance, natural frequencies, and physical nature and arrangement. Comprehensive mathematical studies, substantiated by experimental data, of the individual and collective efforts of these factors and general methods for calculating the magnitudes involved have been advanced by Schurig and Sayre,[4] Dahlgren,[5,6] Eigermann,[7] Pilcher,[8] and Aebi.[9] In shorter articles based on the work of the first-named Schurig, Fricke and Sayre[10]—Tanberg,[11] Specht,[12] Edson,[13] and others[14] have presented certain charts, nomograms, and short cuts that facilitate the numerical labor incident to actual calculation. In consequence of the work of these men, it may be taken that if the designer has knowledge of the physical make-up of the bus structure and of the mutual electromagnetic forces exerted among the bus conductors, he can readily calculate the forces on the bus supports. Obviously, knowledge of the bus structure is immediately available to the designer. Accordingly, if there are also available to him formulas for calculating the mutual electromagnetic forces, he has all that is essential to calculation of the bus support stresses.

Such formulas are derived in this paper, it being assumed that the conductors are nonmagnetic and of such length that end effects are negligible and carry currents distributed uniformly over the cross sections, and that these cross sections are so thin they may be considered to be comprised of line segments (Figure 1).

Of these four assumptions the first and second are commonly satisfied in practice. Re the third, though skin and proximity effects are always present on a-c busses (however, the latter can be largely annulled by use of Arnold's analysis) at power frequencies and normal conductor spacings, their effect on the magnitudes of the electromagnetic forces is, as Beetz[15] has discussed at length, vanishingly small: whence this assumption introduces error negligible with reference to the accuracy

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required in calculating short-circuit forces and enables solution of an otherwise intractable problem. Finally, the assumption of negligible thickness affords formulas that yield values substantially identical with those calculated from the previously mentioned earlier-derived formula for uniformly thick conductors and yet require but a tithe of the numerical computation—whence the labor involved, the time required, and the possibilities of numerical error are considerably reduced.

The desired formulas obtained—by simple differentiation of the known formula for the inductance of the bus with cross section as in Figure 1—their use is illustrated by the numerical solution of a typical problem in bus design.

Derivation of the General Formulas

As derived by Schwantz and Higgins, the inductance per unit length of a circuit comprised of two identical channels located back to back as in Figure 1 is

$$L=-(y+2x)^{-2}[2[y^2-(D+2x)^2]\log[y^2+(D+2x)^2]-4\{[y^2-(D+x)^2]+2y(D+x)\}\times\log[y^2+(D+x)^2]+8yD\log(y^2+D^2)-4[(y^2-x^2)-2xy]\log(y^2+x^2)-4(D+2x)^2\times\log(D+2x)+8(D+x)^2\log(D+x)+12y^2\log y-8D^2\log D+8x^2\log x+8y(D+2x)\tan^{-1}(y/D+2x)-8[2y\times(D+x)+(D+x)^2-y^2]\tan^{-1}(y/D+x)-8(y^2-D^2)\tan^{-1}(y/D)-8(y^2+2xy-x^2)\times\tan^{-1}(y/x)+4\pi y(y+2x)] \quad (1)$$

From fundamental electromagnetic theory, the energy W in the magnetic field associated with a unit length of the circuit when it carries current I is

$$W=LI^2/2 \quad (2)$$

and the mutual electromagnetic force F exerted between the conductors is

$$F=\partial W/\partial D \quad (3)$$

Substituting equation 1 in equation 2, substituting the resulting expression in equation 3, performing the indicated differentiation, and collecting terms yields

$$F=-4I^2(y+2x)^{-2}[-{}^1/_2(D+2x)\log[y^2+(D+2x)^2]+(D+x)\log[y^2+(D+x)^2]-y\times\log[y^2+(D+x)^2]+y\log(y^2+D^2)-(D+2x)\log(D+2x)+2(D+x)\log(D+x)-2D\log D+y\tan^{-1}(y/D+2x)-2(y+D+x)\tan^{-1}(y/D+x)+2D\times\tan^{-1}(y/D)] \quad (4)$$

Simplicity and increased accuracy of numerical computation stem from combining the logarithmic terms according to the rules for addition and subtraction of logarithms. In such manner we obtain from equation 4

$$F=-4I^2(y+2x)^{-2}[(D/2)\times\log[y^2+(D+2x)^2](D+2x)^2D^{-4}+(D+x)\times\log[y^2+(D+x)^2](D+x)^2[y^2+(D+2x)^2]^{-1}(D+2x)^{-2}-y\log[y^2+(D+x)^2](y^2+D^2)^{-1}+y\tan^{-1}(y/D+2x)-2(y+D+x)\tan^{-1}(y/D+x)+2D\times\tan^{-1}(y/D)] \quad (5)$$

Finally, if we take $a=D/y$, $b=(D+x)/y$, and $c=(D+2x)/y$, equation 5 can be written

$$F=2kI^2/D \quad (6)$$

wherein k is a dimensionless parameter to be calculated either from

$$k=[-2Dy/(y+2x)^2][(a/2)\times\log[c^2(1+c^2)/a^4]+b\log[b^2(1+b^2)\div c^2(1+c^2)]-\log[(1+b^2)/(1+a^2)]+\tan^{-1}(1/c)-2(b+1)\tan^{-1}(1/b)+2a\times\tan^{-1}(1/a)] \quad (7)$$

or

$$k=[-2Dy/(y+2x)^2][(a/2)\times\log[c^2(1+c^2)/a^4]+b\log[b^2(1+b^2)/c^2\times(1+c^2)]-\log[(1+b^2)/(1+a^2)]-\tan^{-1}c+2(b+1)\tan^{-1}b-2a\times\tan^{-1}a+\pi(a-b-0.5)] \quad (8)$$

equation 8 being obtained from equation 7 by use of the trigonometric identity, $\tan^{-1}X+\tan^{-1}(1/X)=\pi/2$.

Alternatively, if we take $A=y/D$, $B=(D+x)/D$, and $C=(D+2x)/D$, equation 5 can again be written in the form of equation 6, but therein k is now given by

$$k=[-2D^2/(y+2x)^2][{}^1/_2\log(A^2+C^2)\times C^2+B\log[B^2(A^2+B^2)/C^2(A^2+C^2)]-A\times\log[(A^2+B^2)/(A^2+1)]+A\tan^{-1}(A/C)-2(A+B)\tan^{-1}(A/B)+2\tan^{-1}A] \quad (9)$$

or

$$k=[-2D^2/(y+2x)^2][{}^1/_2\log(A^2+C^2)\times C^2+B\log[B^2(A^2+B^2)/C^2(A^2+C^2)]-A\times\log[(A^2+B^2)/(A^2+1)]-A\tan^{-1}(A/C)+2(A+B)\tan^{-1}(A/B)-2\tan^{-1}A-\pi\times(0.5A+B-1)] \quad (10)$$

Inasmuch as a, b, c, A, B, C, and k are dimensionless, we note that in calculating these quantities x, y, and D may be measured in any unit of length; but otherwise the units of all quantities mentioned to this point are in the absolute system: x, y, and D in centimeters; the current I in abamperes; L in abhenrys per centimeter of bus length; W in ergs per centimeter of bus length; F in dynes per centimeter of bus length.

Inasmuch as in actual practice the dimensions x, y, and D commonly are stated in inches, the current I in amperes, and the force F in pounds per foot, it is convenient re numerical computation to have an equation equivalent to equation 6 wherein, however, the various quantities are measured in the units just stated. Accordingly, introducing the necessary conversion factors in equation 6, we obtain

$$F=(5.4kI^2/D)10^{-7} \text{ pounds per foot of bus length} \quad (11)$$

wherein k, as before, is given by equations 7, 8, 9, or 10.

Regarding these formulas for k, we may remark that though each involves calculation of the sum of a number of logarithmic and arctangent terms—six in all—the actual labor required is not heavy. Thus, using the 15-place tables of natural logarithms and arctangents (with 0.0001 and 0.001 increment arguments) published by the Work Projects Administration[16] and effecting numerical calculations on a ten-digit electric calculating machine, the writer was able to effect the computation pertinent to the following illustrative example in but a few minutes.

An Illustrative Example

The conductors of a single-phase bus are standard aluminum channels (web, $y=8$ inches; flange width, $x=2.56$ inches; thickness 0.524 inches) spaced back to back, $D=16$ inches. To calculate k:

By virtue of the given data we have: $a=2$, $b=2.34$, and $c=2.64$. Substituting these values in equation 8 and performing the indicated calculation yields

$$\begin{aligned}k&=-(1/0.6724)[\log 3.4715578-2.32\times\log 1.6169046-\log 1.27648-\tan^{-1}2.64+6.64\tan^{-1}2.32-4\tan^{-1}2-0.82\pi]\\&=-(1/0.6724)[1.2446034-2.32\times(0.4805136)-0.2441063-1.2087089+6.64(1.1638255)-4(1.1071487)-0.82(3.1415927)]\\&=-(1/0.6724)[-0.5999026]\\&=0.892+\end{aligned}$$

Alternatively, $A=0.5$, $B=1.16$, and $C=1.32$. Substituting these values in equation 9 and performing the indicated calculation yields

$$\begin{aligned}k&=-(1/0.3362)[0.5\log 3.4715578-1.16\times\log 1.6169046-0.5\log 1.27648+0.5\tan^{-1}0.3780303-3.32\times\tan^{-1}0.431034+2\tan^{-1}0.5]\\&=0.892+\end{aligned}$$

Obviously, since it is only desired to know k to two or at the most three decimal places, it is not necessary to use seven-place logarithms and arctangents. Inasmuch, however, as 15-place tables were employed and the calculation was done on a ten-digit calculating machine, the added labor is negligible, and one has no uncertainty as to the effect of cumulative errors on the accuracy of the first few digits of k.

References

1. The Transmission of Alternating-Current Power With Small-Eddy Current Losses, A. H. M. Arnold. Institution of Electrical Engineers (London) *Journal*, volume 80, 1937, pages 395–400.

2. Design of Direct-Current and Low-Frequency Bus Systems; A Guide to the Literature, T. J. Higgins. *Proceedings* of the Midwest Power Conference, 1943.

3. Formulas for Calculating Short-Circuit Forces Between Conductors of Structural, Shape, T. J. Higgins. AIEE Transactions, volume 62, 1943, October section, pages 659–63.

4. Mechanical Stresses in Bus-Bar Support, O. R. Schurig, M. F. Sayre. AIEE Transactions, volume 44, 1925, pages 217–37.

5. Elektrodynamiska Krafter hos Ström forande Ställverksskenor, F. Dahlgren. *Tek-*

The Nature of Vibration in Electric Machinery

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Synopsis: Vibration in electric machinery is the result of a number of contributing agencies, some mechanical, others electromagnetic in origin. Features of mechanical and electrical design and construction which give rise to these agencies are discussed. The general character of the vibration which each agency produces is explained, as well as how different agencies combine to produce the resultant machine vibration. Where excessive vibration is encountered in the field, methods are outlined for ascertaining the major agencies responsible so that corrective modifications may be made which will be effective in reducing the resultant vibration. These methods are applied to motor-generator sets driven by two-pole induction motors to ascertain the cause of a particular type of vibration characterized by the vibration amplitude increasing and decreasing in uniform cycles twice during each revolution of motor slip. In this way the agencies which give rise to this particular type of vibration are explained, and methods by which they may be controlled to keep the resultant vibration to a satisfactory minimum are discussed.

DURING the past few years the progress made in reducing vibration in electric machinery has resulted in more freedom from mechanical failures and other maintenance troubles. The more quiet machines which result from reduced vibration also produce less nervous strain to the operators. The generally accepted theory of vibration is based on the premise that vibration and noise arise from forces which act in a periodic manner upon the different component parts of the machine. Such forces are called "driving forces," and the actual mechanical displacement or vibration which results is frequently called the "vibrational response." The amplitude of the vibrational response depends upon the amplitude of the driving forces as well as upon the mass, stiffness, and damping factors associated with the vibrating member. While this fundamental concept is quite simple, yet to account for particular types of vibration which are produced in electrical equipment and mechanical rotating assemblies is sometimes quite difficult.

In general, the amplitude of a vibrational response can be reduced either by decreasing the amplitude of the driving forces which cause it, or else by modifying the mass, stiffness, and damping factors associated with the vibrating member so that a smaller response occurs under the action of the same driving forces. In electric machinery there are many agencies which contribute to these factors and to the final vibrational response. As a rule it is not a simple process to isolate the particular agencies responsible for excessive vibration, particularly when several agencies are cumulative in effect and are present simultaneously. A careful systematic study based upon a knowledge of conditions which are conducive to excessive vibration and the frequency components which arise from them usually is necessary before effective corrective measures can be specified.

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The driving forces which are present in electric machinery are of two general kinds, namely, mechanical and electromagnetic. Mechanical driving forces are those which arise from mechanical or structural irregularities such as mechanical unbalance, nonsymmetrical mechanical parts, friction, loose bearings, and so forth. Electromagnetic driving forces are those which arise by virtue of magnetostriction and magnetic attraction between different parts of the machine. In any electric machine both kinds of driving forces are present simultaneously and act together to produce the vibrational response.

A very important agency which contributes to excessive vibration is static or dynamic unbalance of a rotating assembly. This produces an excessive mechanical driving force of fundamental frequency—that is, numerically equal to the speed of rotation in revolutions per second—and this results in an excessive vibrational response of the same frequency. This phenomenon has long been realized and many kinds of balancing machines, some of which are quite elaborate, have been developed for testing and correcting the condition of balance of rotating parts. For this reason this factor is not considered in detail here. It is mentioned merely because other types of vibration which arise from altogether different agencies frequently are attributed to improper balance. Mechanical unbalance alone always gives rise to a vibrational response having a frequency which is numerically equal to the speed of rotation in revolutions per second. This fact very often can be used to differentiate between excessive vibration due to mechanical unbalance and excessive vibration due to other agencies. Components of vibration having frequencies which are multiples of the speed of rotation in revolutions per second cannot be reduced appreciably by improvement in the conditions of balance of rotating parts, because the driving forces which produce them originate through other agencies.

Another well-known cause of excessive vibration which must be dealt with in machine designs is closely related to mechanical resonance in the rotating assembly and is referred to as the phenomenon of ordinary critical speeds. When the speed of rotation is such that the frequency of the driving force due to mechanical unbalance corresponds with one of the natural resonant frequencies of the rotating assembly, the latter is in indifferent equilibrium, and a small component of driving force may cause excessive vibration and whipping, or even mechanical failure, of the rotating assembly. For satisfactory operation the ordinary critical speeds must be consider-

nisk Tidskrift Elektroteknik, [7], volume 55,1925, pages 117–21.

6. ELECTRODYNAMICAL FORCES IN BUS BARS, F. Dahlgren. *Electrical World*, volume 86, 1925, pages 473–4.

7. CALCUL DES FORCES INTÉRIEURES DÉVELOPÉES DANS DES SYSTÉMES MÉCANIQUES SOUMES À DES EFFORTS ÉLECTRODYNAMIQUES, A. Eigermann. *Revue Générale de l'Électricité* (Paris, France), volume 35, 1934, pages 677–88.

8. SHORT-CIRCUIT FORCES ON BUS BARS AND THEIR SUPPORTS IN CELLULAR SWITCHGEAR STRUCTURES, E. E. I. Pilcher. *World Power*, volume 24, 1935, pages 118–23, 190–2, 231–4.

9. UNTERSUCHUNGEN ÜBER ELEKTRODYNAMISCHE BEANSPRUCHUNG, M. F. Aebl. *Schweizer Archiv für Angewandte Wissenschaft und Technische*, volume 7, 1941, pages 185–95.

10. PRACTICAL CALCULATION OF SHORT-CIRCUITS STRESSES IN SUPPORTS FOR STRAIGHT PARALLEL BAR CONDUCTORS, O.R. Schurig, C. W. Frick, M. F. Sayre. *General Electric Review*, volume 29, 1926, pages 534–44.

11. STRESSES IN BUS SUPPORTS, R. Tanberg. *Electric Journal*, volume 24, 1927, pages 517–25.

12. SHORT-CUT METHODS OF CALCULATING STRESSES IN BUS STRUCTURES, W. Specht. *General Electric Review*, volume 31, 1928, pages 413–18.

13. SHORT-CIRCUIT STRESS CHARTS, W. M. Edson. *Electrical World*, volume 94, 1929, page 1125.

14. BUS STRESSES UNDER SHORT CIRCUITS. *Electrical World*, volume 92, 1928, pages 564–5.

15. DIE STROMKRAFT BEI PARALLELEN UND KONZENTRISCHEN LEITERN, W. Beetz. *Electrotechnik und Maschinenbau* (Vienna, Austria), volume 48, 1930, pages 151–4.

16. TABLES OF NATURAL LOGARITHMS; TABLES OF ARCTANGENTS (book). Work Projects Administration, New York, N. Y.

ably outside the normal operating speed range of the machine. This subject has received careful attention by many investigators who have developed methods whereby the critical speeds may be computed from the dimensions and component masses of the assembly.

Mechanical Driving Forces

The fundamental component of driving force which arises from a rotating assembly is caused by static or dynamic unbalance. It has a frequency numerically equal to the speed of rotation in revolutions per second. The amplitude of this driving force may be controlled by careful adjustment of balance of the rotating parts. In addition to the fundamental component, harmonic driving forces having frequencies which are multiples of the speed of rotation in revolutions per second frequently occur. These are caused by mechanical imperfections or dissymmetries in the construction of the machine. For this reason they are more difficult to control than the fundamental. The second harmonic component—that is the one having a frequency double that of the fundamental—is most likely to give rise to troublesome vibration. This component arises from shafts having portions of noncircular cross-sectional area, caused by keyways, for instance, from slightly elliptic bearings, as well as from other mechanical imperfections. Elliptic bearings are more likely to be a source of trouble in the case of ball bearings as avoidance of slight warping of the bearing races is practically impossible during the heat-treatment processes necessary in their manufacture. Slightly oval or nonspherical balls also may cause harmonic driving forces; but these are usually of higher harmonic order than the second and are not necessarily integral multiples of the fundamental. The only effective method by which harmonic driving forces arising from these causes may be controlled is by the use of rotating assemblies and shaft designs which are as nearly circular in cross section as practical considerations will permit, and by better workmanship and closer tolerances in the manufacture of the component parts of the machine. Harmonic driving forces must be given special consideration from the standpoint of vibration and noise in machines of special design where the rotating assembly is unusually long if the shaft has a keyway or other dissymmetries. They may be serious also if the inherent resonant frequencies of the machine frame correspond closely with the frequency of the harmonic driving forces.

Electromagnetic Driving Forces

Electromagnetic driving forces arise in electric machinery for several reasons, but the principal fundamental causes may be traced to magnetostriction and magnetic attraction between different parts of the magnetic circuit of the machine. Magnetostriction plays an important role in creating vibration and noise in static apparatus such as transformers. In rotating machinery the forces caused by magnetic attraction are by far the more important. In general these forces are quite complex in nature, consisting of numerous harmonic components. The frequency of the most important component, at least with respect to amplitude, is twice the frequency of the voltage at the machine terminals, and for this reason will be called the second harmonic component of electromagnetic driving force. Other harmonic components of driving force arise because of the slots in the stator and rotor of the machine. The

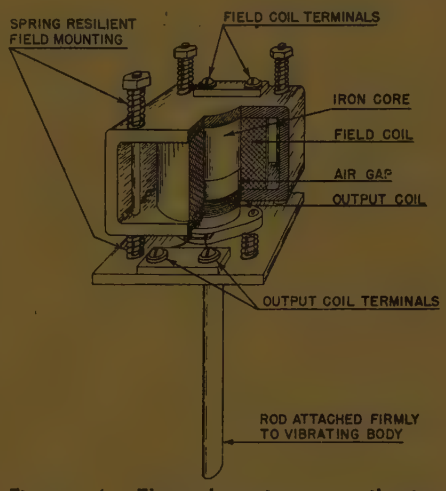


Figure 1. Electrodynamic-type vibration pickup unit

order of these harmonic components is a function of the number of slots on both the stator and the rotor. Certain of these are attributed to the machine windings not being infinitely distributed, while others exist simply because of the stator and rotor teeth. Certain combinations of stator and rotor slots result in much more pronounced driving forces than do others, and methods have been developed for choosing the optimum combinations for minimum noise. As a rule these harmonic components are of comparatively high order and are of importance in vibration principally from the standpoint of noise.

The second-harmonic component of electromagnetic driving force always is present in an electric machine. It arises principally from the radial magnetic attraction between the stator and rotor. Nearly all rotating machines make use of revolving magnetic fields. As the magnetic field revolves in the air gap of the machine, it passes a given point of the stator twice during each cycle of voltage change, once as a flux entering the stator and once as a flux entering the rotor. A force of attraction results regardless of the flux polarity. This force therefore builds up and dies away twice during each cycle of voltage change and produces a driving force having a frequency exactly twice that of the voltage supply. This driving force causes a periodic distortion of the stator core which, through the agency of the stator mounting supports, acts as a driving force to excite vibrations in the frame of the machine. This driving force cannot be eliminated since it is inherent in any electric machine, but the vibration or response it causes can be controlled by constructing the stator core sturdy enough so that the resultant core response is small, or in special cases such as large alternators (15,000 kva and larger) by mounting the stator core on flexible supports so that minimum vibration is transmitted to the frame of the machine.

The second-harmonic component of driving force is much more pronounced in two-pole machines than in machines having a greater number of poles, because the distance along the stator periphery between points where the flux is simultaneously at maximum values is comparatively much greater. This is one of the fundamental reasons why two-pole motors present more difficult problems from the standpoint of vibration than do motors having a greater number of poles. Forces of magnetic attraction act simultaneously upon diametrically opposite points of the stator and cause it to become distorted into an elliptic form, the distortion progressing around the stator in synchronism with the magnetic field.

In addition to the factors already mentioned, harmonic driving forces result from air-gap dissymmetries. The type of dissymmetry most frequently encountered is imperfect centering of the rotor in the stator core. This gives rise to a second-harmonic electromagnetic driving force in a two-pole machine. As the magnetic flux in the air gap revolves, it passes the point where the air gap is shortest twice during each cycle of voltage change, once as a flux entering the rotor and once as a flux entering the stator. Simultaneously, a flux of opposite polarity exists in the air gap at the point diametrically opposite. Since the radial magnetic force exerted on the rotor increases as the air-gap length decreases, the two attractive forces acting at diametrically opposite points of the rotor do not always cancel. When the flux is in the position where the air gap is longest or shortest, a net force results which acts toward the point where the gap is shortest. As the air-gap flux moves from the position, the unbalance between the two diametrically opposite forces diminishes and becomes zero at the point 90 degrees from this position. As the flux rotates further, an unbalance again exists in the same direction and reaches a maximum when the flux has moved through another 90 degrees. This unbalance of radial forces builds up and dies away twice

during each cycle of voltage change and produces a second-harmonic driving force which acts to deflect the rotor toward the point where the air gap is shortest. This driving force is transmitted through the bearing supports and serves as a driving force to produce vibrations in the frame of the machine.

This driving force may be eliminated by perfect centering of the rotor within the stator core. It also may be used in some instances to counteract or neutralize driving forces of the same frequency which arise from other causes, since the phase and magnitude of the driving force thus produced can be controlled by the use of an adjustable bearing support which will allow the rotor to be shifted off center in any desired direction. The magnitude of this driving force depends upon the amount by which the rotor is off center with respect to the stator, and the phase (with respect to the impressed voltage) depends upon which direction it is off center, that is, upon the angular position of the point in the air-gap periphery where the gap is shortest.

The use of this method for controlling vibration has several disadvantages from the standpoint of actual practice. Each machine of a like series must be adjusted independently to secure the maximum benefit. Also, if the machine is at all critical with respect to its mounting, an adjustment correctly made at the factory or for one location in service would not be correct for any other location or mounting. This method can be used to advantage, however, in emergencies and other instances where a better method cannot be readily applied.

Combined Effect of Electromagnetic and Mechanical Driving Forces

During the operation of an electric machine, the vibration or response which results from several driving forces may be considered from the standpoint of superposition. Each harmonic component of driving force will produce a component of response of the same frequency as that of the driving force. The amplitude of each component of the response depends upon the magnitude of the driving force and upon the mass, stiffness, and damping factors associated with the component parts of the machine. The resultant total response at any point of the frame is equal to the sum of the harmonic components of the response. If there is a correspondence in frequency between one or more of the driving forces and the natural or resonant frequencies of the component parts or complete assembly of the machine, comparatively large amplitudes of vibration will result, consisting principally of frequencies for these particular modes of vibration.

As a rule, excessive vibration due to resonance can be recognized by the behavior of the vibration accompanying changes in frequency of the driving forces. In the neighborhood of resonance, the vibration will build up to a maximum amplitude at the resonant frequency and decrease as the frequency of the driving forces is either increased or decreased from this value. Mechanical driving forces produced by a rotating assembly may be changed in frequency by changing the speed of rotation. Electromagnetic driving forces may be changed in frequency by changing the frequency of the voltage supply. Sometimes the two kinds of driving force are dependent upon one another as in the induction motor. In such cases the frequency of both can be changed simultaneously by a change in supply frequency.

In cases where mechanical resonance is prominent, the only effective method of reducing the resultant vibration is a change in the machine design which will shift the natural resonant frequencies inherent in the machine assembly outside the range of frequencies encountered during normal operation so that mechanical resonance will not occur. To insure this result in a new design is not easy because the geometric shapes of machine frames and other parts do not lend themselves to accurate mathematical analysis. It is very difficult, except in special cases, to compute the natural resonant frequencies of a machine frame or assembly. The final test of a design is the nature of the vibrational response which results under the action of the driving forces present under normal operation of the completed machine. If high amplitudes of vibration do not arise from small driving forces, the design is satisfactory. Otherwise stiffening of certain parts and modifications in the shape of others are necessary. Additional stiffness may be secured by increasing the size of the members, or by the use of cross braces of one form or another which increase the rigidity. The use of steel of higher tensile strength will have little or no effect, since the stiffness depends upon the modulus of elasticity which is essentially the same for all grades of steel regardless of the ultimate tensile strength.

From the standpoint of excessive vibration which results in excessive bearing wear, failure of mechanical members, and other maintenance troubles, as contrasted with noise, frequencies in the lower audio range are of major importance. For the lower resonant frequencies the stiffness and damping factors of mechanical members are comparatively much smaller, and a much greater resonant response can result. Fortunately, very low resonant frequencies are not usually encountered in actual practice in machines of standard design, because the frame and other parts must be constructed with sufficient strength to withstand stresses which are likely to arise during both normal and abnormal conditions of operation. For this reason the natural resonant frequencies inherent in the assembly usually will be of comparatively high order as compared with the driving forces produced by the machine in operation. Exceptions to this general rule occur in very large machines, and in compact high-speed motor-generator sets having comparatively long armatures. This is particularly true in machines of fabricated-steel-frame construction. In this latter instance considerable rigidity is sacrificed for the lighter construction thus

Figure 2. Vibration of motor generator set under load with voltage at rated frequency applied to the motor

A. Actual vibration as recorded by means of electrodynamic vibration pickup unit
B. Same as A except high-frequency components filtered out of recorded wave



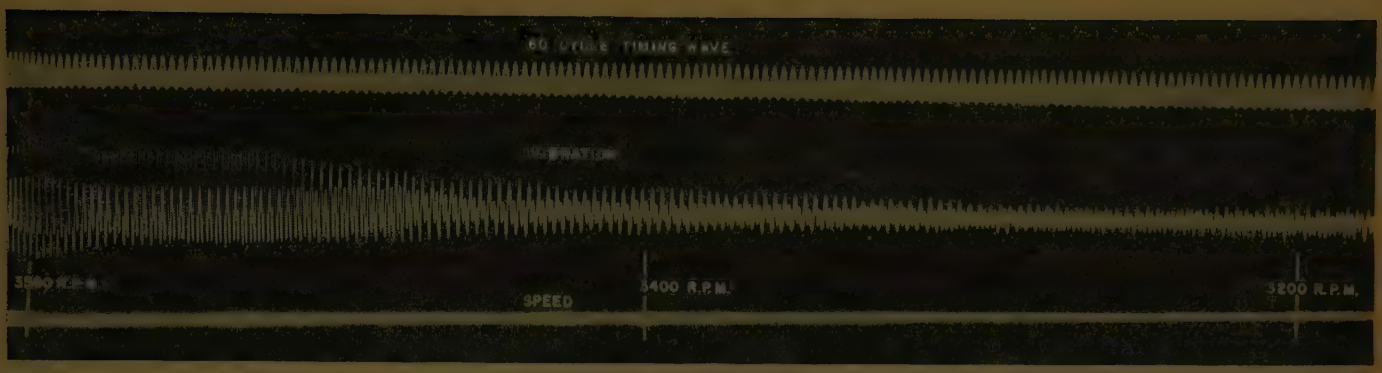


Figure 3. Vibration of motor generator set coasting to rest without load and without generator excitation

afforded, and the lowest natural frequency may be of the order of 120 cycles per second or even less.

In the case of two-pole 60-cycle induction motors, the important electromagnetic driving force of lowest frequency is 120 cycles per second. At the same time a mechanical driving force, the second harmonic component, which has a frequency double the speed of rotation in revolutions per second, can exist. When these two driving forces exist simultaneously, they alternately reinforce and interfere with each other and cause a response which builds up and dies away in amplitude giving rise to the phenomenon of beats. Since the difference in frequency is exactly twice the slip frequency of the induction motor, two cycles of the beat frequency correspond exactly with one revolution of motor slip. If the lowest resonant frequency of the machine frame is in the neighborhood of 120 cycles per second, the vibration amplitude may build up to an excessively high value even though the driving forces are comparatively small. The beats are then very pronounced and clearly audible. The only effective manner of reducing vibration in this case is to modify the machine frame to avoid mechanical resonance, and perhaps also to modify the machine construction to reduce the amplitude of the driving forces. If the machine is of a portable nature such that the method of mounting or arrangement of support on its foundation may be changed, or if the machine operates over a fairly large speed range, the frame would have to be made more rigid, rather than less rigid, in order to avoid possible operation in the resonant range. Increasing the rigidity has the effect of raising the resonant frequencies. Appreciable increase in rigidity cannot be obtained by the use of the same frame design and steel of higher tensile strength. Frame members of higher moment-of-inertia cross section must be used.

Experimental Studies and Observations

The ideas presented in this paper are the result of considerable research which has been conducted on motor-generator sets in which the complete rotor assembly of both motor and generator was mounted on a single shaft supported by two ball bearings, one at either end of the shaft. The complete set was housed by a single fabricated steel frame of welded construction. The driving motor was a 3,600-rpm two-pole squirrel-cage induction motor. The particular type of vibration produced in these sets was characterized by the amplitude of the vibration building up and dying away twice during each cycle of motor slip, thus producing a very decided beat which was clearly audible. The beat is clearly distinguishable in most of the oscillograms of the accompanying figures which were obtained by means of an electrodynamic vibration pickup unit. The signals so obtained were amplified by one stage of voltage amplification, followed by a push-pull power-amplifier stage, the output of which was recorded by means of a mechanical oscillograph.

Figure 1 is a pictorial drawing of the pickup unit. The field coil was excited with direct current to produce a flux in the air gap in which the small output coil was mounted. The field coil and its associated magnetic circuit were supported on comparatively weak springs to allow free motion between this assembly and the output coil. The small frame which carried the output coil was rigidly attached to or else firmly held against the mechanical member when the vibration of the member was being recorded. The principle of operation of the pickup unit is similar to that of the dynamic type microphone, except that it is mass controlled. The electromotive force generated in the output coil is proportional to the relative velocity of this coil with respect to the field-coil magnetic circuit. The output wave form represents the instantaneous velocity of the vibrating member.

The actual mechanical displacement can be obtained from the velocity wave by taking the derivative, or else by resolving the wave into its sinusoidal components, dividing each component by its own angular frequency, combining the resultant terms, and interpreting the wave to the proper scale. As far as actual mechanical displacement is concerned, the velocity wave accentuates the high frequencies. If the noise produced by a machine is the factor of principal concern, the instantaneous sound pressure is more closely related to the instantaneous acceleration. The acceleration wave may be obtained from the velocity wave by one step of integration, or else by taking the sinusoidal components of the velocity wave, multiplying each by its own angular frequency, combining the resultant terms, and interpreting the resultant wave to the proper scale. The velocity wave represents an average of two extremes, and thus forms a good basis for comparing relative vibration amplitudes. It may be used very satisfactorily in studying the effects of different factors on the amplitude and nature of the vibration produced in a machine.

In order to eliminate the effects of stray electromagnetic fields which were very prominent in the proximity of the machines used in this investigation, the vibration pickup unit and the amplifier had to be very carefully shielded, both electrostatically and electromagnetically. The power amplifier stage was of the transformer input type and presented a difficult problem from this standpoint, because of the transformer connected to the grids of the output tubes. To reduce stray pickup from this source to a negligible value, the transformer was enclosed in a $^3/_{16}$-inch-thick soft-steel shield, and the amplifier was placed several feet from the machine under test.

Figure 2A is a representative oscillogram of the actual vibration which was obtained by the use of this apparatus. Careful examination of the pattern shows that the wave consists principally of a component of slightly less than 120 cycles per second which varies in amplitude having two maximum and two minimum values for each revolution of slip of the induction motor. In addition, the wave contains numerous harmonic components, some of rather high order, which are of fairly constant amplitude and bear no apparent relationship to the slip of the motor. The frequency of several of these harmonic components was measured with a sound analyzer and found to check with the frequencies to be



Figure 4. Oscillograms showing the effect upon the machine vibration of simultaneous changes in the motor supply frequency and speed

A. Machine speed 3,400 rpm
B. Machine speed 3,800 rpm
C. Machine speed 3,900 rpm
D. Machine speed 5,000 rpm

For comparing vibration amplitude of D with amplitudes of A, B, and C, divide by two

expected from the stator and rotor teeth of the machine. For these reasons, a capacitor was connected across the output terminals of the voltage amplifier stage to by-pass the high-frequency components and filter them out of the recorded wave. Figure 2B is an oscillogram of the same vibration recorded in Figure 2A, but with the filter capacitor connected into the amplifier circuit. Beats in this oscillogram are very pronounced, showing that the wave contains two principal components of approximately equal amplitude. By analysis, the frequency of one component is exactly 120 cycles per second, and the frequency of the other is exactly double the speed of rotation of the motor in revolutions per second.

The presence of the 120-cycle component of vibration is easily explained on the basis of electromagnetic driving forces produced in the machine as previously described. The component having a frequency twice the speed of the motor is due to mechanical causes only. This is shown clearly by the oscillogram of Figure 3. This oscillogram was obtained by starting the motor without load and allowing it to reach maximum no-load speed. Then the motor voltage supply was switched off and the machine allowed to coast to rest without load and without electrical excitation of any kind. The vibration was recorded while the machine was coasting to rest. The mechanical vibration has a maximum amplitude at approximately 3,580 rpm and contains a very large second harmonic component. This component decreases in amplitude quite markedly as the speed decreases, and becomes negligible at approximately 3,300 rpm.

That the beat vibration reached a high amplitude because of mechanical resonance is demonstrated by the following test. The motor of the set was supplied from an adjustable frequency source and operated at different speeds by adjustment of the source frequency. The beat would become discernible when the speed was approximately 3,300 rpm, increase in intensity with an increase of speed above this value, and reach a maximum intensity at approximately 3,560 rpm. As the speed was increased further, the beat would come less prominent, and would be barely discernible at 3,900 rpm. It was completely absent at 5,000 rpm. This behavior is illustrated by the oscillograms of Figure 4, together with that of Figure 2B. These oscillograms were obtained at speeds of 3,400 rpm, 3,800 rpm, 3,900 rpm, 5,000 rpm, and 3,530 rpm, respectively. For a comparison of relative amplitudes in Figure 4, the ordinates of oscillogram D are to be divided by two, since this oscillogram was obtained with the oscillograph adjusted for double the sensitivity used for recording the other three.

To ascertain the part of the machine responsible for resonance, the rotor assembly first was examined critically. The first ordinary critical speed was computed analytically by several methods from the

dimensions and masses of the rotor assembly. Different methods gave values ranging from 5,500 rpm to 6,200 rpm. As a final check, the rotor assembly was removed from the machine and mounted on rigid supports at the bearings. The natural frequency of vibration of the rotor assembly was measured by recording the free vibration of the rotor assembly under impact. Figure 5 is an oscillogram of the vibration at the center of the rotor assembly caused by a light blow from a rubber hammer. The blow was applied at the quarter-point of the rotor assembly. The frequency of the free vibration is 104 cycles per second, which corresponds with a first ordinary critical speed of 6,240 rpm. In actual operation of this machine, the bearings were not rigidly supported. This lack of complete rigidity somewhat lowered the critical speeds. By observation, the secondary critical speed due to gravity actually occurred between 2,800 and 2,900 rpm, which would fix the first ordinary critical speed at approximately 5,700 rpm. With the first ordinary critical speed of the rotor in this speed range, it is impossible to attribute the resonance phenomenon which occurred at 3,560 rpm to any resonant condition in the rotor assembly.

Impact tests similar to those described above were made to determine resonant frequencies in the machine frame. The complete frame, which was rectangular in shape, was mounted on three rigid point supports which could be moved to different relative positions. Blows were applied at different points of the frame, with and without the rotor assembly in place; and the vibration resulting at various points of the machine was recorded and studied. The nature of the free vibration depended upon the relative location of the three fixed point supports, the point where the blow was applied, and the point where the vibration was recorded. It made no apparent difference whether the rotor was in place or removed. The oscillograms of Figure 6 and Figure 7 are typical of the results obtained. Figure 6 shows a very prominent 115-cycle component and Figure 7 a very prominent 125-cycle component. In obtaining the oscillogram of Figure 6, two of the three point supports were placed at extreme corners of the frame at one end of the machine, and the third point support was placed at the center of the opposite end. For Figure 7, symmetry of the supports with respect to the frame was maintained, but the supports were placed considerably closer together. The actual mounting of the machine corresponded to a condition intermediate between these two and gave a resonant frequency of approximately 119 cycles per second.

As a final check on considerations regarding the cause and nature of the beat vibration, the frame of the machine was stiffened by welding several pieces of angle iron to the frame. The machine was operated at rated voltage and frequency, and the vibration was studied under different conditions of motor load, and with various arrangements of the three rigid frame supports. After reinforcement of the frame, the beat was completely absent at many points of the frame regardless of the arrangement of the rigid frame supports. At other points the beat could be detected, but was not prominent. At points where the beat was most prominent, the vibration was similar in character to that of Figure 4C. At other points, the vibration consisted almost entirely of a component slightly less than 60 cycles per second, which was due to slight mechanical unbalance of the rotating assembly. That the beat vibration was produced by the combined effect of electromagnetic and mechanical driving forces and reached a high amplitude because of resonance in the frame is clearly indicated by these tests and observations.

Conclusions

Vibration and noise in electric machinery arise by virtue of both mechanical and electromagnetic driving forces which exist simultaneously during normal machine operation. The amplitude of the vibrational response which results depends upon the amplitude of the driving forces, and also upon the mass, stiffness, and damping factors of the component parts of the machine. To secure quiet operation and comparative freedom from vibration, all of these factors must be dealt with. When excessive vibration is present in a machine of a particular design, the factors or agencies which contribute most to this condition must be ascertained, and modifications in design and construction made to minimize them. Because of inherent design features, two-pole induction machines and high-speed motor generator sets present more difficult problems from the standpoint of vibration than do machines having a greater number of poles which operate at lower speeds. Certain mechanical and structural irregularities of the rotating assembly may produce harmonic driving forces, and in particular a mechanical driving force having a frequency which is double the speed of rotation in revolutions per second. This driving force, acting simultaneously with an electromagnetic driving force having a frequency double that of the voltage supply, causes a resultant vibrational response which builds up and dies away in amplitude twice during each revolution of motor slip. This "beat vibration" may be very troublesome, particularly if the frame of the machine has an inherent resonant frequency which is approximately double the frequency of the voltage supply. A beat vibration of this kind may be reduced by careful attention to mechanical irregularities which give rise to the second harmonic component of mechanical driving force, or, if resonance is a contributing factor, by modifying the frame of the machine so that resonance does not occur. The second-harmonic component of electromagnetic driving force is inherent in the machine and cannot be eliminated; but it may be reduced with respect to the frame by the use of a stiffer stator core, or by the use of a special stator mounting which will not transmit the core response to the frame in cases where such refinements are justified. Whenever excessive vibration is encountered in electric machinery, a compre-

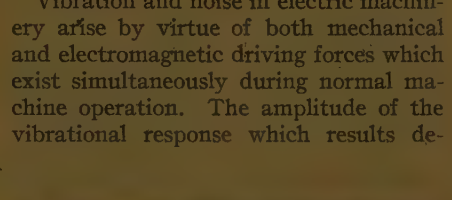


Figure 5. Free vibration of machine rotating assembly produced by a sudden blow from a rubber hammer

Assembly was mounted on rigid supports at the bearings

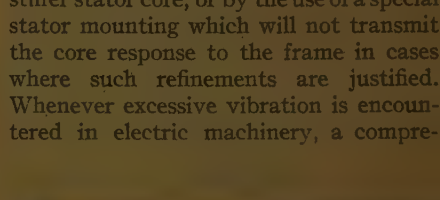


Figure 6. Free vibration of machine frame produced by a sudden blow from a rubber hammer

Frame was supported by three rigid point supports, one at each corner of one end of the frame, and one at the center of the opposite end of the frame

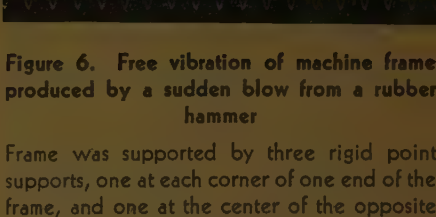


Figure 7. Free vibration of machine frame produced by a sudden blow from a rubber hammer

Frame was supported by three rigid point supports, two near one end of the frame, and the third near the opposite end of the frame

hensive study based upon a consideration of the frequency of the components of vibration present and the agencies which give rise to them will point the way to proper corrective measures and modifications which will be effective in reducing the resultant vibration.

References

1. Measurement of Noise From Small Motors, C. G. Veinott. AIEE Transactions, volume 53, 1934, December section, pages 1624–8.

2. Vibration-Measuring Instruments, C. D. Greentree. AIEE Transactions, volume 56, 1937, June section, pages 706–10.

3. Recent Developments in Balancing Machines, C. R. Soderberg. ASME *Transactions*, volume 45, 1923, pages 111–30.

4. A New Type of Dynamic-Balancing Machine, E. L. Thearle. ASME *Transactions*, volume 54, 1932, APM-54-12, pages 131–41.

5. Dynamic Balancing of Small Gyroscope Rotors, O. E. Esval, C. A. Frische. AIEE Transactions, volume 56, 1937, June section, pages 729–34.

6. Methods of Rotor-Unbalance Determination, J. G. Baker. ASME *Transactions*, volume 61, 1939, pages A-1 to A-6.

7. Vibration of Frames of Electrical Machines, J. P. Den Hartog. ASME *Transactions*, volumes 50–51, 1927–28, APM-50-6 and APM-50-11.

8. Stress Analysis in Electrical Rotating Machinery, M. Stone. ASME *Transactions*, volumes 50–51, 1927–28, APM-50-16, pages 57–79.

9. Improved Method for Calculating Free Vibrations in Systems of Several Degrees of Freedom, W. M. Dudley. ASME *Transactions*, volume 60, 1938, pages A-61 to A-66.

10. Theory of Elastic Engine Supports, S. Rosenzweig. ASME *Transactions*, volume 61, 1939, pages 31–6.

11. Critical-Speed Behavior of Unsymmetrical Shafts, H. D. Taylor. ASME *Transactions*, volume 62, 1940, pages A-71 to A-79.

12. The Effect of Foundation Stiffness on the Resonant Frequencies of Rotating Machines, E. H. Hull. ASME *Transactions*, volume 63, 1941, pages A-121 to A-129.

13. Induction-Motor Slot Combinations, G. Kron. AIEE Transactions, volume 50, 1931, pages 757–67.

14. Effect of Skewing and Pole Spacing on Magnetic Noise in Electrical Machinery, S. J. Mikina. ASME *Transactions*, volume 56, 1934, pages 711–20.

15. Un Problème de Vibrations en Électrotechnique: Supports Élastiques pour les Machines Électriques, C. Galmiche. *Revue Générale de l'Électricité* (Paris, France), volume 35, June 1934, pages 739–51.

16. The Cause and Elimination of Noise in Small Motors, W. R. Appleman. AIEE Transactions, volume 56, 1937, November section, pages 1359–67.

17. Special Problems of Two-Pole Turbine Generators, C. M. Laffoon, B. A. Rose. AIEE Transactions, volume 59, 1940, January section, pages 30–3.

18. Harmonic Theory of Noise in Induction Motors, W. J. Morrill. AIEE Transactions, volume 59, 1940, August section, pages 474–80.

19. Suppression of Magnetic Vibration and Noise of Two-Pole Turbine Generators, A. L. Penniman, Jr., H. D. Taylor. AIEE Transactions, volume 60, 1941, June section, pages 283–8.

20. Field Harmonics in Induction Motors, M. M. Liwschitz. AIEE Transactions, volume 61, 1942, November section, pages 797–803.

21. Steam and Gas Turbines (book), Aurel Stodola. McGraw-Hill Book Company, New York, N. Y., 1927.

22. Mechanical Vibrations (second edition), J. P. Den Hartog. McGraw-Hill Book Company, New York, N. Y., 1940.

Electrothermal Space Heating and Air Conditioning

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SPACE heating of dwellings has been one of the prime necessities in the improved living conditions of the human race and one that has received considerable study from the earliest time. As the various heating methods were developed, from the early open fireplace to modern heating equipment, utility and esthetic designs for the heating equipment were given a great deal of consideration.

In Sweden very artistic heating units were developed approximately 150 years ago of ceramic glazed tile, which were extremely attractive in design as well as being useful. These devices used small quantities of fuel to heat the entire mass of tile, and the heat energy stored in this tile was radiated into the room, furnishing the required heat for the day.

In the last 20 years in the Pacific Northwest, experiments have been conducted continually, and many practical installations which utilize electric energy for heating have been made. However, these installations have been almost entirely of the radiant and convection type of heater. This type of heater requires an uninterrupted supply of electric energy. In 1935 at the city of Grand Coulee, adjacent to Grand Coulee Dam, an electric heat-storage installation was constructed and placed in operation utilizing granite boulders for the heat-storage medium. This installation was operated for approximately two years on an experimental basis, the results of which experiment were outlined fully in a report by the State College of Washington experiment station.[1] Also in the Pacific Northwest, several installations of storage heaters utilizing hot water are in operation with varying degrees of success.

The problem of filling valleys and reducing peaks in daily load curves of utility systems has existed since the early days of electric distribution. Daily load factor has a direct bearing on cost of service. Increasing load factor decreases cost of service. Incentive rates have been established by utilities in order to encourage use of electric service during off-peak hours, as witnessed by special rates established for hot-water heating and the development of devices for controlling use of electricity during predetermined hours. The present-day prices charged for electric energy used by consumers in the Pacific Northwest are low enough to have encouraged serious consideration of the adoption of electric space heating.

The increased cost of oil, coal, and wood fuels, and the severe curtailment of oil is an incentive for the use of electric energy in space heating.

These conditions led the author to consider the development of a method for utilizing an electric-energy–heat-storage system which would permit the use of off-peak electric energy obtained principally during the night hours

In designing the electrothermal storage system for space heating, consideration was given not only to the heating but also to the problem of air conditioning in as simple a manner as possible, bearing in mind that the electrical method was the one offering the greatest possibilities for regions having low-cost hydroelectric energy as available in the Pacific Northwest. Cleanliness was also a prime factor to be considered. It was decided that a medium for heat storage should be selected which offered extreme flexibility and provided for the storage of a large amount of heat energy in the minimum of space. Water under moderate pressures and temperatures seems to offer the best solution to this problem. Therefore, a system was devised wherein water is stored in an unfired pressure vessel under a pressure of 200 pounds per square inch and at the saturated temperature corresponding to this pressure. Electrothermal space heating is based on fundamental thermodynamic principles, when a common fluid (water) is used for the storage of heat generated by electric energy. The heat is stored in the fluid under pressure. As the pressure is reduced, the sensible heat of the fluid is converted into latent heat of vaporization. The vapor carries this heat to the heat exchanger and liberates the latent heat when condensation takes place.

By the use of this method of off-peak heat–energy storage, economical use of a utility's distribution system can be accomplished. The present distribution systems of the various utilities are being

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utilized at a relatively low load factor. Greater use of these facilities during the periods when the load factor is low permits the fullest possible use of the system with only minor expansion of the distribution facilities. Any added use of these facilities is a source of additional revenue to the utility with only slight additional cost. Another very important factor of the off-peak storage system is that it does not require the utility to guarantee uninterrupted service. This factor has caused the utilities in general to back away from space-heating loads, because additional equipment is required on their part to render this uninterrupted service. Normal outages in no way would affect the heating of the dwelling, if the storage system is in use. As a general thing, the rate structure of a utility is set up to carry all fixed charges and operating expenses plus a reasonable profit on the distribution of energy at a moderate load factor. Therefore, the additional energy distributed over their existing facilities without much additional cost to them is very desirable. The revenues derived from this additional energy will result in a greater net income, which in turn can be shared with the users of electric energy, thus benefiting both the utility and the user.

With reference to Figure 4, a typical daily load curve of the Pacific Power and Light Company (main system) for December 16, 1943, shows the night load between midnight and 6:30 a.m. to be less than half of the peak load during the evening hours from 5:00 p.m. to 7:00 p.m. On this load curve the morning peak between 8:00 a.m. and 12:00 noon is slightly lower than the two-hour peak between 5:00 p.m. and 7:00 p.m. However, some of the systems reverse this order and have a higher peak during the morning hours, but the duration of both morning and evening peaks are approximately as shown on this load curve. It is the object of the off-peak electrothermal heating system to use as much as possible of the available energy represented by the valleys in this curve for space-heating purposes. The shaded section above the load curve represents the energy that would be used by 100 homes similar to the one that was chosen as a demonstration unit at the Midway substation of the Bonneville Power Administration, which is located in the area of central Washington served by the Pacific Power and Light Company. It is estimated that the off-peak energy represented by the valleys of this load curve could be used to heat 1,500 five- or six-room dwellings.

The electrothermal off-peak heat-storage system was designed to meet all of the safety and insurance requirements of the state of Washington, the city of Seattle, and the city of Tacoma, as well as the code of the American Society of Mechanical Engineers for unfired pressure vessels. All electric equipment used in this system has been approved by the national Underwriters' Laboratories.

Principal Equipment of the Electrothermal Off-Peak Storage System

A general description of the electrothermal storage-space-heating and air-conditioning equipment consists of the following:

The main items of equipment are a superheated hot-water accumulator, immersion electric heaters providing the source of heat, a reducing valve for automatically releasing the heat energy from the accumulator to a lower pressure level, a heat-transfer element for transmitting the heat energy into the air used for heating the dwelling, a condensate receiver to store the condensate until such a time as it is returned to the accumulator, an electrically controlled feed pump, a thermostat for maintaining a constant predetermined temperature in the dwelling, a humidistat for maintaining the relative humidity at a predetermined level, renewable dust filters, activated carbon filters, and an air-circulating fan.

Method of Operation of Electrothermal Off-Peak Storage System for Winter Heating

A schematic diagram of the electrothermal off-peak accumulator system is shown in Figure 1. A description of the method of operation of this system is outlined here.

The accumulator *A* is an unfired pressure vessel filled 90 per cent full of water under normal operating conditions. When fully charged the pressure in the accumulator is maintained at 200 pounds per square inch and 388 degrees Fahrenheit. The accumulator is built according to the specifications of the latest unfired

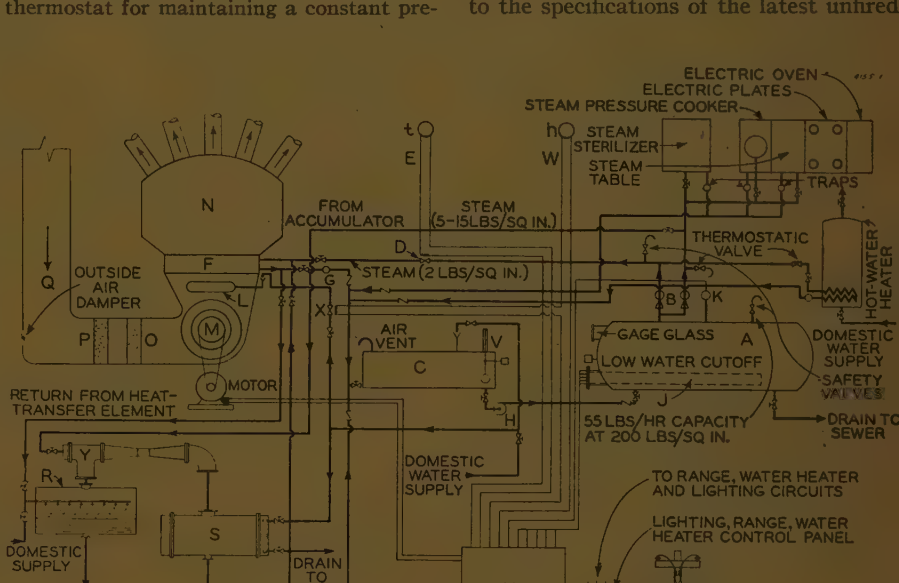


Figure 1. Schematic diagram of complete electrothermal off-peak accumulator heating and air-conditioning system

Equipment:

A. Electrically heated water tank and accumulator. Normal operating condition 90 per cent full
B. Steam-pressure reducing valve
C. Condensate receiver
D. Modulating thermostat control valve
E. Room thermostat and time clock
F. Heat-transfer element
G. Condensate return trap
H. Feed pump
J. Immersion electric heaters
K. Pressure control
L. Humidifier
M. Air fan
N. Plenum air chamber
O. Activated carbon filter for air purification
P. Removable air filter for dust
Q. Return air duct from rooms
R. Evaporator
S. Condenser
T. Chilled-water pump
U. Control panel
V. Float switch in condensate receiver
W. Room humidistat
X. Humidity-control valve
Y. Thermocompresser unit

Specifications:

All electrical controls to be Underwriters' approved standard. Accumulator to be approved ASME unfired pressure vessel for 200 pounds per square inch and 388 degrees Fahrenheit. Valves, fittings, and piping to be American standard for 200 pounds per square inch working steam pressure

pressure vessel Code as published by the ASME, which insures a pressure vessel having a high factor of safety for use in dwellings. The vapor above the water in the accumulator is released through a pressure-reducing valve *B*, which reduces the pressure from 200 to 2 pounds per square inch. The two-pound per square inch steam line is provided with a safety valve in order to limit the pressure in this part of the system to not more than five pounds per square inch. Thermostatic control valve *D*, which is under control of the room thermostat and night setback time clock *E*, regulates the flow of steam

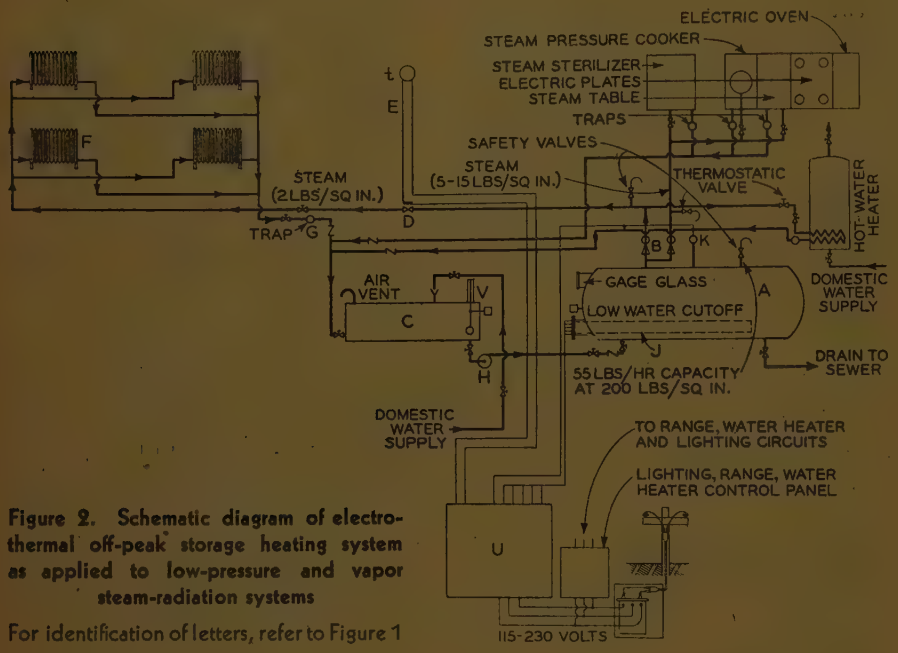


Figure 2. Schematic diagram of electrothermal off-peak storage heating system as applied to low-pressure and vapor steam-radiation systems

For identification of letters, refer to Figure 1

to the heat-transfer element *F* as governed by the room temperature. Valve *D* is a modulating valve permitting continuous flow of varying quantities of steam as required and is not the on-and-off type. As the steam is condensed in the heat-transfer element, the flow of condensate is controlled by a thermostatic trap *G*, which allows only the liquid to pass. The condensate is returned to the condensate receiver *C*, where it is stored until such a time as the liquid level has risen to a predetermined point. At this predetermined point the float switch *V* energizes the feed-pump circuit and permits the feed pump *H* to return the condensate to the accumulator *A* at the slow rate of approximately 0.25 gallon per minute. The condensate cannot be returned to the accumulator unless the heating elements *J* are energized. This low rate of feed to the accumulator was fixed, so that this water could absorb nearly all of the required heat energy from the immersion electric heaters, rather than deriving it from the high-temperature water in the accumulator. It is evident that with this closed circuit there will be little or no loss of water which would require an appreciable amount of make-up water. Because of the closed circuit there is no accumulation of solids from the water to form objectionable scale in the system. Safety features in connection with the hot-water accumulator are a safety valve with outside discharge, which has a steam-flow capacity considerably in excess of the heat-generating capability of the electric immersion heaters *J*, and an electric pressure control *K* which interrupts the power circuit to the electric heaters when the pressure in the accumulator has reached the predetermined pressure of 200 pounds per square inch. Air required to transmit the heat from the heat-transfer element to the rooms of the dwelling is propelled through the heat-transfer element into the plenum chamber *N* by the forced-air-circulating fan *M*. From this point air is conducted to the rooms of the dwelling by well-designed and well-constructed air ducts. The return air from the rooms is conducted through the return air ducts *Q*, and is passed through removable dust filters *P*, which remove all dust and lint from the air. This air then passes through activated carbon filters *O*, which remove all household odors. Outside air is admitted to the suction side of the fan in sufficient quantities only to maintain the oxygen and the carbon-dioxide contents of the air at normal levels. By recirculating the air in the system, with only this small addition of outside air, considerable heat is saved, and a slight positive pressure inside is maintained, thus reducing infiltration of outside air. To maintain the proper relative humidity in the dwelling, a humidifier *L* is provided. The moisture for humidification is provided by a fine spray nozzle that injects the water into the air stream in the form of a mist. This mist then passes through the heat exchanger and is completely vaporized and mixed with the heated air. The flow of water to the spray nozzle is through valve *X*, which is under the control of room humidistat *W*. To govern the flow of electric energy to the system, time switches are provided to determine the point at which the energy is cut off during the peak period of the day and then re-established during the night hours. A second time switch is provided which will permit a limited amount of energy to be taken during the off-peak period between 12:00 noon and 5:00 p.m. The controls are operated so that the various heaters are energized in groups; therefore, the entire heater load cannot be thrown on the system at one time. This function is controlled by a motor-driven step controller which permits the energization of the first four kilowatts of heater capacity, then the next six kilowatts of heater capacity, and finally the last four kilowatts. Should the demand for heat during the day vary from the maximum, the step controller will reverse its direction of rotation and de-energize the heaters in steps in order that the predetermined pressure of 200 pounds per square inch in the accumulator will not be exceeded.

Method of Operation of Electrothermal Off-Peak Storage System for Summer Cooling

For summer cooling, the steam supply to and condensate supply from the heat-transfer element are shut off by the closing of valves in those lines, and the two valves controlling the chilled water to and from the heat-transfer element are open, and a summer switch governing the automatic controls is placed in the summer position. This automatically energizes the summer cooling equipment and controls. The cooling is accomplished by circulating chilled water through the heat-transfer element *F*. This chilled water is produced by the use of a small vacuum water-refrigerating unit which comprises a thermocompressor unit, condenser, and evaporator. The steam from the accumulator is utilized to operate the thermocompressor *Y*, which together with secondary steam-jet ejectors produces a vacuum in evaporator *R*. Return water from the heat-transfer element is sprayed into the evaporator, which operates at a high vacuum causing the evaporation of a portion of the return water. This evaporation removes the heat from the balance of the return water, reducing its temperature to approximately 45 degrees Fahrenheit. The water vapor evaporated, plus the steam supplied to the thermocompressor, is carried over to the condenser *S*, which is cooled by

circulating water from the domestic water supply. The condensate from the condenser is returned through a vacuum trap to the condensate receiver *C*, and thus reenters the closed circuit to the accumulator. The chilled water from the bottom of the evaporator is circulated by a chilled water pump *T*. This pump is controlled by the room thermostat *E*, thus maintaining a temperature within the dwelling approximately 15 degrees Fahrenheit below the outside air temperature. As the entire system is controlled automatically, after being adjusted, it requires little attention for operation.

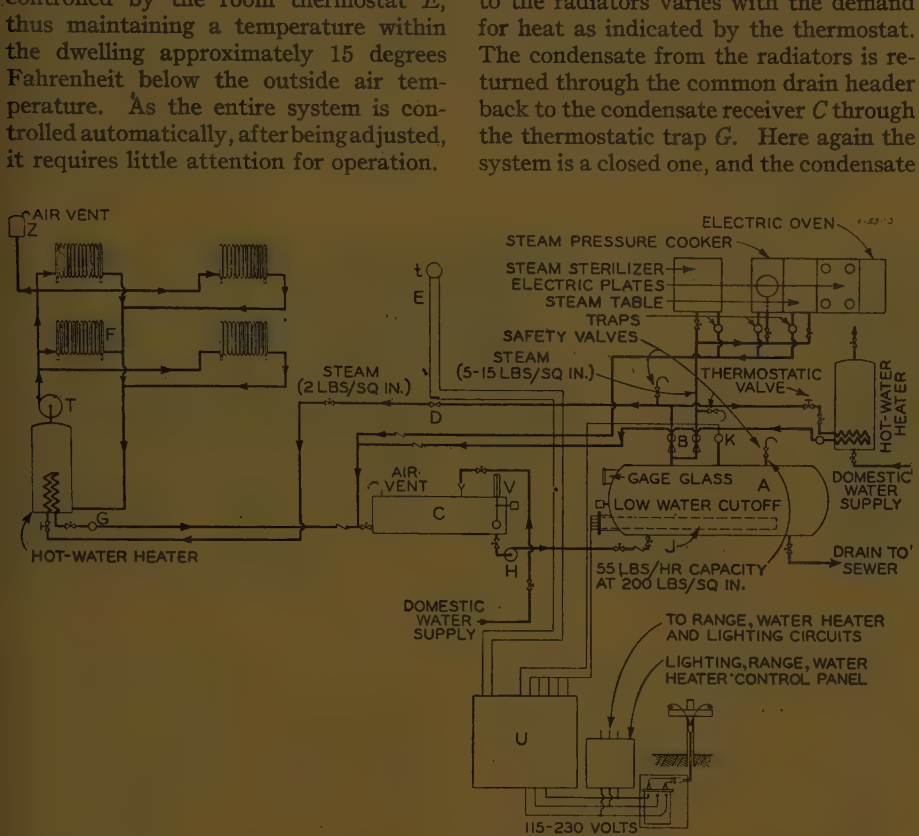


Figure 3. Schematic diagram of electrothermal off-peak storage heating system as applied to gravity and forced-circulation hot-water heating systems

Equipment:

A. Electrically heated water tank and accumulator. Normal operating condition 90 per cent full
B. Steam-pressure reducing valve
C. Condensate receiver
D. Modulating thermostat control valve
E. Room thermostat and time clock
F. Hot-water radiation
G. Condensate return trap
H. Feed pump
J. Immersion electric heaters
K. Pressure control
T. Hot-water pump, either forced or gravity circulation
U. Control panel
V. Float switch in condensate receiver
Z. Expansion tank

With an accumulator system of this kind in a dwelling, advantage can be taken of the stored heat energy to provide sterilizing steam for use in the kitchen, as well as to provide a source of steam-heat energy for pressure cooking of food and for a steam table to maintain prepared food at a uniform temperature, thus making available to the occupant of a small dwelling all of the conveniences that now are available in the most modern hotel and restaurant kitchens.

Low-Pressure Steam Installations

Figure 2 shows adaptability of the electrothermal heating system to existing dwellings having low-pressure steam-heating installations.

The electrothermal heating unit can be used as the source of heat energy for existing dwellings that have installations of low-pressure steam radiators *F* for heating. It is not considered necessary to repeat the description of the various elements in the electrothermal heating system, as these have been described earlier in this paper. In order to adapt the electrothermal storage heating system to a low-pressure steam-radiator installation, it is necessary only to connect the two pounds per square inch steam-discharge header from the hot-water accumulator to the main steam riser of the existing system. The main thermostat *E* will respond to the room temperature and regulate the steam flow through the modulating-thermostat-control valve *D*. By this method of control the steam flow to the radiators varies with the demand for heat as indicated by the thermostat. The condensate from the radiators is returned through the common drain header back to the condensate receiver *C* through the thermostatic trap *G*. Here again the system is a closed one, and the condensate is continually re-used with only slight make-up water being required. With this low-pressure steam-heating system, no control of relative humidity or air conditioning is possible unless additional ventilating ducts and equipment are installed. The various uses to be made of the steam in the kitchen and for hot-water heating remain as previously described.

Hot-Water Heating

Figure 3 shows the two general types of hot-water-heating systems that are installed in existing dwellings, namely, gravity systems and forced-circulation systems. The use of the electrothermal off-peak accumulator system with either type of the previously mentioned installations is practically as simple as that used in the low-pressure steam radiator system. In order to adapt the electrothermal accumulator system to hot-water heating, it is necessary to provide only a small hot-water tank fitted with an immersion steam coil. This coil functions as a heat-transfer element to heat the water of the radiator system. The flow of steam to this immersion coil is controlled by the modulating thermostatic control valve *D*, which in turn is controlled by room thermostat *E*. The circulation in the gravity hot-water system is comparatively slow, which makes it difficult to maintain a uniform room temperature. If the hot-water system is of the forced-circulation type, a more even distribution of the water is obtained throughout the system, and the water temperature can be governed more uniformly by the thermostat. The hot-water

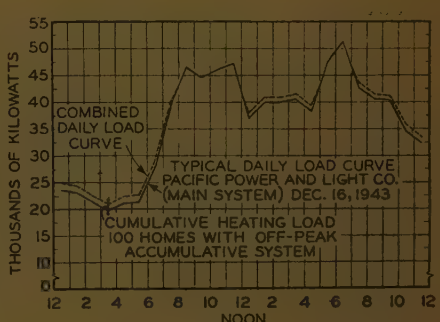


Figure 4. Typical daily load curve of a utility system

Effect of space-heating load on Pacific Power and Light Company. 100 homes—accumulator system

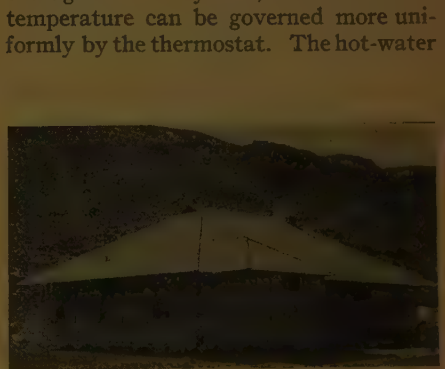


Figure 5. Chief operator's cottage at Midway substation

heating systems do not provide humidification nor air conditioning. In order to obtain these features it would be necessary to provide humidifying and ventilating equipment.

Midway Cottage Installation

An electrothermal off-peak heat-storage and air-conditioning system has been installed recently in a five-room cottage at the Midway substation of the Bonneville Power Administration in central Washington (see Figure 5).

The cottage has no basement. Therefore, to conserve space in the dwelling, the entire electrothermal and air-conditioning unit was installed in the attic.

The cottage is well-constructed with insulated floors, side walls, and ceilings. All windows are tightly weather-stripped, and both front and back entrances are vestibuled with double doors. The design conditions at Midway are minus five degrees Fahrenheit outside and 70 degrees Fahrenheit inside, with a computed heat loss for the dwelling of 35,000 Btu per hour.

All of the heating equipment is housed in a small well-insulated room constructed in the attic. This room is easily reached through a hatch from the utility room on the first floor.

The electric control board was placed on the outside wall of the small equipment room to make it accessible for testing purposes. This board is shown in Figure 6.

The cold air return duct system was installed under the floor, and the hot air ducts were placed over the ceiling joist. The hot air is distributed from the duct system through registers or grills placed near the ceiling line, affording a very even method of distribution of the air in the dwelling.

Two electrothermal hot-water accumulators were used in this installation instead of a single larger one in order to secure the required heat-storage capacity and yet be small enough to pass through a 24-inch by 24-inch hatch to the attic. The arrangement of the equipment in the room is compact yet accessible for maintenance purposes.

Hot-water accumulators are insulated with $4^1/_2$ inches of magnesia block and 4 inches of rock wool and then covered with duck which is pasted on.

The entire duct system is insulated with rock wool, which is wired on and finished by covering with duck.



Figure 6 (left). Control panel



Figure 7 (right). Accumulators, pressure-reducing equipment, and electric control equipment

Figure 7 shows the equipment installed inside the attic room.

Each of the two accumulators contains 104 gallons of water. At the normal operating condition the accumulators have an effective heat-storage capacity of 300,000 Btu, between 200 pounds per square inch and 2 pounds per square inch.

The Midway installation incorporates all of the features discussed in connection with schematic diagram, Figure 1, with the exception of the refrigerating unit. This will be installed as soon as it is completed. Weather conditions have been too warm to obtain any scientific data on this heating system.

Conclusions

Electrothermal space heating and air conditioning is based on thermodynamic principles and is very simple. The equipment involved in the construction of the system is standard and can be supplied by existing manufacturers.

Heating and heat-storage features can replace oil, coal, or wood-burning equipment used in existing heating systems in dwellings, such as hot-air heating, Figure 1; low-pressure and vapor-steam heating, Figure 2; and gravity or forced-circulation hot-water systems, Figure 3. It is also possible to make use of the stored heat energy for sterilizers, pressure cooking, and steam tables, giving the housewife all the advantages of the modern hotel kitchen.

The system is practical and economical to install in any dwelling or building now existing or in new construction. It is estimated that the cost for converting a hot-air heating system to the electrothermal storage heating system in a six-room dwelling would not exceed $600.

The electrothermal heat-storage unit uses daily off-peak electric energy, thus using hydroelectric energy that otherwise would be wasted in stream-flow plants.

This system is advantageous from the standpoint of the utilities, as it permits the use of their distribution systems at a much higher load factor than is now possible. It also offers the advantage that an interruption to the service for short periods of time, which are unavoidable, has no effect on the operation of the storage system.

The consumer derives advantages from the storage system by being assured of a continuous heat supply. Then there are advantages of cleanliness, elimination of all fire hazards, lower insurance rates, simplicity of operation, and minimum space requirements.

Studies indicate that the cost of the equipment installed in the dwelling is moderate and competitive with other first-class heating and air-conditioning equipment on the market at the present time.

Reference

1. The Feasibility of Using a Heat Storage Device for Domestic Heating With Electricity, Homer J. Dana, R. E. Lyle. State College of Washington *Bulletin*, volume 18, July 1935.

An Investigation of the Relationship Between Temperature and Movement of Cables in Ducts

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MANY complex thermal and mechanical influences govern the relationship between electrical loading and the movement of cable in an underground conduit line. It is probable therefore that a knowledge of the relationship will come only through the analysis of many careful observations of the movement of cable in conduits of different types and in different locations and also under the influence of electrical loadings of various magnitudes and durations. It is apparent that engineers now appreciate the importance of a knowledge of the factors that influence cable movement as well as of the control of these factors as a requisite for higher loading of cables that operate under the load factors normally encountered.

This paper contributes to the general information that is accumulating on the subject the results of tests made over a five-month period in the underground distribution system in Buffalo, N. Y. It also describes the testing methods and the equipment used in the hope that this information will prove helpful to others who may wish to conduct similar tests. Finally the paper advances a theoretical explanation of the relationship between the electrical loading and the mechanical movement of cable. The test results support the theory, but more data are required for a confirmation. It is hoped that the presentation of this theory will stimulate the assembly of additional data.

Description of Conduits and Cables on Test

About 8,600 feet of 350,000-circular-mil 25-kv cable, located in a typical underground conduit line, was operated on a controlled-current loading schedule which permitted the recording of the expansion and the correlated sheath temperature rise over a period of about five months. The load cycle for most of the tests was equivalent to a load factor of less than 30 per cent, although some data were obtained for load factors approaching 100 per cent.

The site of the test was a 4,300-foot conduit run in an area considered typical of underground-system conditions in Buffalo. The conduit installation is less than 15 years old. It contains 16 four-inch fiber ducts installed in a concrete envelope with the individual ducts on seven-inch centers and arranged four ducts wide and four ducts high. Most of the manholes are ten feet long, six feet wide, and about seven feet deep. The entire line is located beneath asphalt street paving.

The general arrangement of the test conduit line is shown in plan and profile in Figure 1. There are 17 sections between manholes, and individual sections vary from less than 100 feet to over 400 feet in length. Some of the sections are in rock, whereas other sections are above the rock line. Some of the sections are submerged in ground water, some are what might be termed moist, and a few are relatively dry.

The conduit is not subjected to external heat influences and carries only three three-conductor 350,000-circular-mil, 25-kv cables all located in the bottom row of ducts. Two of these cables, in adjacent ducts, were used for the tests. The remaining cable carried no load while the test was in progress. The cables manufactured in 1930 have regular sector conductors which are insulated with $^{20}/_{64}$ inch of impregnated paper and shielded with three-mil-thick copper tape over the insulation. The insulated conductors are bound together with a five-mil-thick steel binder. The lead sheaths are $^{9}/_{64}$ inch thick. The complete cable has an over-all diameter of $2^{7}/_{8}$ inches and weighs about 12 pounds per foot.

These cables, installed in their present location in 1931, have attained sheath surface dullness and rest in the ducts in a normal fashion. They have never been loaded above a small fraction of their normal carrying capacity. They are arranged rather well in the manholes with the splices centrally located on hangers approximately 40 inches apart. The splicing sleeves are 28 inches long and $5^{1}/_{2}$ inches in diameter. The two cables included in the test are carried through the manholes on the same 17-inch arms. Radii of the bends in the manholes vary considerably but fall in the general classification of 15 inches at the duct mouth and 30 inches at the support arm with a total offset from the line of ducts of 18 inches.

Description of Test Equipment

The two test cables, located in adjacent ducts, were connected in series, and the three conductors were connected solidly together at one end of this test circuit. The power supply to the test circuit was provided through a three-phase low-voltage source with variable-voltage control, capable of providing up to 380 amperes per phase to the test cables. This maximum current produced roughly 18 watts loss per foot of cable.

Time-clock control was provided so that the test current was initiated and interrupted on a selected time schedule. The test-current magnitude, however, was manually preselected with the result that unit cable losses varied somewhat as the test-cable conductors heated up on each load cycle.

Cable-sheath temperature was measured by a special probe which fits into the same duct occupied by the cable. The probe is fitted with an "advance-copper" thermocouple which is carefully shaped to make a large-area contact when it is pressed against the cable sheath by the action of a bellows. The couple is mounted on a small bakelite plate which is attached rigidly to the formed end of a small-diameter steel pipe. A small metallic bellows is attached to the pipe probe on the side opposite the couple. Figure 2 shows the couple end of the probe.

The probe is inserted, couple end first, into the duct containing the cable to be checked. When the couple is in the duct six or seven feet from the manhole duct entrance, and contact between sheath and couple is evident from an electrical-continuity test between couple and cable sheath, a pressure of 15 pounds per square inch is applied to the bellows by way of the pipe probe. The bellows presses against the duct wall and forces the couple against and slightly into the cable sheath.

Sheath temperature is read by using the thermocouple in combination with a potentiometer which is compensated for ambient temperature. During the tests the temperature measurements were taken at intervals of roughly 30 minutes and always at the close of each test. Unattended recording equipment was not used because of the possibility of losing thermal contact with the cable as a result of cable expansion. However, the probe was left in the occupied duct between readings to reduce effect of thermal lag.

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Table I. Summary of Temperature

Curve	Date of Measurements (1943) From	To	Current in Amperes	Duration of Current in Hours	Manhole 145 (East) Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Manhole 171 West Sink Temp—C	Max Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Manhole 171 East Sink Temp—C	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Manhole 205 West Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Manhole 205 East Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm
A	5-13	5-20	260	4	23	13	4.5	10	24.5	14.5	4.5	10	21.5	10.8	3.5	25.7	13.5	4.5	20	6.8	2.0
E	6-10	6-10	260	7			6.0				5.5				4.0			6.0			4.0
F	6-14	6-14	260	4			4.5				4.5				3.0			4.0			2.5
G	6-15	6-15	300	4			7.0				7.0				5.0			6.0			4.5
H	6-16	6-16	380	4			15.5				13.0				11.5			15.0			10.0
I	7-26	7-27	260	4			4.5				4.5				3.5			4.5	32.0	6.5	2.5
J	8-30	8-30	260	4																	
K	8-31	8-31	300	4			7.2				6.8				6.5			6.0			5.5
L	9- 1	9- 1	380	4			15.0				14.5				12.0			15.0			11.0
M	9- 7	9- 7	260	4			5.0				4.5				3.5			5.0			3.0
N	9- 8	9- 8	300	4			8.0				7.0				7.0			7.5			5.5
O	9- 9	9- 9	380	4			15.0				14.5				12.0			14.0			12.0
B	9-14	9-23	260	4	30	12	5.0	18.5	33	14	5.0	18.5	29.5	10.5	4.0	35.3	13.8	5.0	28.0	7.5	4.0
C	9-28	10- 1	325	72	48	29	16.0	17.5	51	32	16.5	17.5	44	25	15.0	51.0	30	14.5	38.2	18.7	12.0
D	10- 8	10-14	300	149	44	26	15.0	16.5	48.5	30	14.0	16.5	42	24	14.0	48.5	29	12.0	38.0	19.0	10.0

* Four to eight-watt-per-foot heaters in adjacent ducts in section from manhole 317 east to manhole 347 west from 9-7-43 to end of tests. Heaters on same time cycle as test cables.

A continuous record was made of the expansion and contraction of the test cables at the duct entrances in the manholes. This was accomplished through the installation of graphic recorders made up from old graphic demand instruments. A flexible shaft, attached to the cable at the duct edge, transmits cable motion to the recorder mechanism and pen and provides a continuous record of cable motion on paper strip charts. This arrangement of the recorder is shown in Figure 3.

Mercury-bulb thermometers were used to obtain sink and empty-duct temperatures. A small-diameter steel pipe about four feet long was driven through the manhole wall about four feet below grade and at right angles to the line of the conduit run to accommodate the thermometer for sink measurements. The locations at which sink temperatures were measured are shown in Figure 1.

Test Schedule

Before starting the tests, each manhole was carefully inspected, all subnormal arrangements of the cables were corrected, and all fireproofing was removed from the cables. The field survey was started by taking photographs of the interior of all of the manholes by means of special photographic equipment which fitted into the manhole-cover openings in such a way that all pictures taken of each manhole were from the same camera position and angle of exposure. This made possible a direct comparison of photographs to determine the changes in the positions of the cables, splices, supports, and so forth, and for correlation with field measurements taken during the tests. The advantage of this care in the photographic work is shown in Figure 4, where a picture taken during a severe load cycle is superimposed on one taken after the testing was completed. Figure 5 shows the photographic equipment itself.

Bending radii and location dimensions were taken to establish the position of the cables at the start of the tests. Current then was applied in two four-hour 260-ampere cycles each day. This loading, four hours on and eight hours off in each 12-hour period, was about double the 24-hour load factor for which ratings were desired, but at the low heating level no carry-over of heat between the individual cycles occurred. This loading developed about seven watts per circuit foot of cable.

Temperature observations during the first few load cycles showed appreciable increases in only six sections. Expansion recorders were installed on the 12 ends of these six sections. The balance of the conduit line was either quite moist or partly flooded.

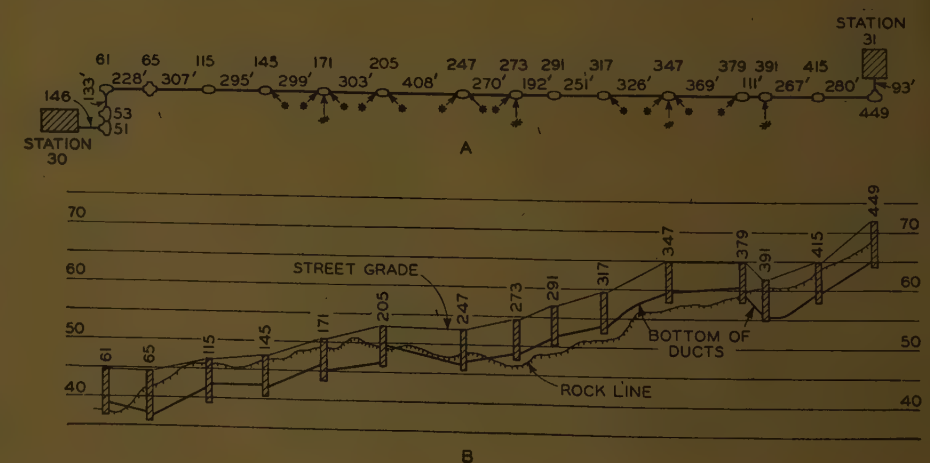
With a few interruptions for spot checks, the test schedule of two 260-ampere four-hour cycles every day was continued from May 13 to August 11. From that date to September 26 the schedule was changed to one four-hour 260-ampere load cycle per 24-hour period. During this latter schedule special heating cables, each having losses of eight watts per foot, were installed in four ducts adjacent to the test cables in one of the sections which showed a large temperature rise during the earlier tests. The purpose was to simulate the effect of additional loaded cables in this section of conduit by the addition of losses equal to 32 watts per foot. The heating cables were energized and de-energized on the same schedule as the cables under test.

A few special tests were made at higher values of loading and some for longer durations in order to obtain data on the thermal characteristics of the conduit system. These data subsequently proved

Figure 1. Plan and profile of the test conduit line

* indicates location of sheath-temperature thermocouple and cable-expansion recorder

indicates location of earth temperature thermometers



Manhole 247 West			Manhole 247 East			Manhole 273 (West)				Manhole 317 (East*)			Manhole 347 West*				Manhole 347 East				Manhole 379 (West)			
Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sink Temp—C	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sink Temp—C	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mmm	Sink Temp—C	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Sheath Temp—C	Sheath Temp Rise—C	Expansion in Mm	Curve
.20	9.5	2.5	19.2	7.3	3.0	11.5	21.5	9.0	3.0	24.3	11.5	4.5	11.2	26	12.5	4.5	11.8	19.8	8.5	2.5	10.3		3.0	*A*
		3.5			3.5				3.5	27.0	12.2	5.0	17	29.5	13.5	5.0	17	25.5	10.5	3.0			5.5	*E*
		3.0			3.0				3.0	26.7	10.2	4.0	17.5	29.5	11.3	5.0	17.5	25.5	8.1	2.5			3.0	*F*
		4.5			4.5				4.5	30.4	12.3	6.5	18	34.5	17	6.5	18	29.0	12.5	4.5			5.5	*G*
		9.5			11.5				8.5	40.5	21.5	14.0	18.5	44.8	24.3	13.0	18.5	35.0	16.5	8.5			8.5	*H*
30.5	8.0	3.0			3.0				2.5	32.6	9.6	4.5	25	36.5	10.8	4.5	25	33.7	7.5	2.5			3.0	*I*
										30.5	9.5		24	33.7	9.2		24	30.5	6.0					*J*
		5.0			6.0				5.0	34.5	13.5	6.0	23.5	38.1	15.4	6.0	23.5	33.7	11.0	4.5			5.0	*K*
		9.0			12.0				9.0	42.5	21.5	15.0	23.8	48.0	24.8	13.0	23.8	41.7	18.5	10.0			9.0	*L*
		3.0			3.0				4.0	32.3	11.6	6.0	24	34.7	12.5	5.5	24	30.5	8.3	3.0			3.5	*M*
		5.0			7.0				5.0	36.7	15.7	9.0	24	40.0	17.2	7.5	24	34.2	11.4	5.0			5.5	*N*
		10 0			12.0				9.0	44.2	22.7	16.0	24	49.7	26.1	14.0	24	41.5	18.3	10.0			9.0	*O*
28.8	7.8	3.5	28.5	8.0	4.5	21.5	29.2	8.2	3.5	32.5	12.5	5.5	21.5	36.8	14.8	5.5	21.5	31.5	9.5	3.5	31	10	3.5	*B*
41.0	20	12.0	40.5	20.5	10.5	19.5	41.2	21.2	9.0	53.2	34.2	23.0	20.3	60.7	39.7	17.5	20.3	44.0	24.0	13.0	44	23	10.0	*C*
38.5	18	10.0	38.0	19	8.0	16.8	41.5	22.0	8.5	52.0	33	25.0	19	61.5	41	16.0	19	42	23.0	11.0	42	22	8.0	*D*

to be very informative in the study of the expansion phenomena.

Test Results

Figure 6 has been prepared as a general picture of the increase and decrease of the linear dimensions of the six cable sections as recorded by the equipment at each end of each section. This information is presented by plotting the recorded positions at the time of maximum longitudinal expansion and maximum contraction for each end of each cable section during each test cycle. These maximum and minimum points are joined together by straight lines except for the two long-duration load cycles and for those cases where the loading was discontinued for appreciable periods. The expansion and contraction curves for these special tests have been plotted on the basis of four points taken from the record during each 24-hour period.

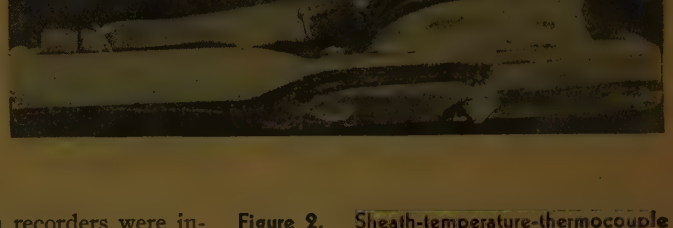
The scale used for longitudinal expansion in Figure 6 indicates cable motion in the manhole. No movement of cable into the duct beyond the starting point was recorded in the six sections where recorders operated.

The gaps in the curves shown in Figure 6 are the result of mechanical difficulties in the operation of the recorders. The data so lost are not of serious consequence.

Table I shows the maximum sheath temperatures measured during each test for the six sections referred to previously. It should be noted that the sheath temperatures given in the tabulation were taken at different loadings, at different ambient temperatures as shown by the sink temperature measurements and also at different load factors. The tabulation gives the expansion of the cable that appeared in the manholes at each end of each section as a result of the measured sheath temperature rise also shown in the tabulation. All of the values for expansion were obtained by scaling the curves of Figure 6.

The variation in the sheath temperature increases at the 12 test points as shown graphically in Figure 7 at four different times and at three different heating levels.

Figure 8 is a graphic report of the total expansion during each test for each of the six sections on which recorders were installed. The data for Figure 8 were obtained by adding together the recorded expansion of the two ends of each of the test sections to obtain the total expansion for each section for each test. The expansion in each case is the increase in length apparent in the manhole for each section and for an individual load cycle, without regard for expansion, permanent set, or other effects which accumulate as a result of such factors as changes in earth temperature and motion of the sections as a whole, which produce long-time changes and irregularities.



The bellows provides the force to insure good thermal contact between the thermocouple and the cable sheath at a point six or seven feet inside the duct

Figure 2. Sheath-temperature-thermocouple probe

Analysis of Tests

The first thing of interest found in analyzing the test results is that the measured temperature rise at the test points is remarkably free from radical variation with time. This is brought out particularly well in the two curves at the bottom of Figure 7 which are plotted from data taken during tests made four months apart.

Another interesting observation from Figure 7 is that the maximum variation in measured temperature rise for the whole group of test points in any test approaches two to one. This is not surprising, but it is important since it could indicate that the variation of temperature over the individual section lengths is considerable and that measurements of temperature taken at the ends of the section may not be a true indication of average section temperature.

However, the comparison of the temperature measurements at the ends of the individual sections for each test shows a range of temperature variation considerably less than that for the whole test run. Furthermore, the relationship between the individual end temperatures is maintained with change in load factor and load level as shown by a comparison of the shape of the upper curves with that of the lower curves in Figure 7.

The plotted temperature rise, Figure 7, has many of the characteristics of the

elevation profile, Figure 1, in that the maximum temperature rise of each section is at the end of maximum elevation. This characteristic, in all probability, is due to the tendency of the higher ends of the duct sections to have the minimum moisture in and around the duct bank.

Turning next to Figure 9, one sees that the shape of the curves for total measured section expansion and that of the curves for the average temperature rise have a very close similarity. It is interesting, however, that Figure 9 shows several marked deviations from this similarity in the two groups of curves. At the heating level existing on the 260-ampere four-hour cycle, the measured section expansion in all cases was less than one millimeter per degree centigrade sheath rise, whereas at the higher heating levels found in the 325-ampere 72-hour and 300-ampere 149-hour tests the section expansion was on the average above one millimeter per degree centigrade. In addition, the two lower expansion curves in Figure 9 show that for the 260-ampere four-hour loading there was considerable variation in the expansion as measured at two different times while the temperature rise showed almost perfect repetition.

The curves in Figure 10 show that the realized elongation of the cable observable in the manholes is less than 50 per cent of the theoretical free expansion of either the lead sheath or the copper conductors.

Fortunately, these three effects, nonlinear relationship of movement with temperature rise, variation with time of year, and failure to approach theoretical elongation, are of interest chiefly in studies aimed at establishing the mechanism of expansion. A given cable installation can be operated satisfactorily with only a general knowledge of the relationship between loading and expansion. This is particularly true, since the arrangement of cable in a manhole is not carried out with scientific precision and with an expectation of permanency. Cable arrangements designed to permit certain top limits of expansion in most cases will deteriorate with time, and only prohibitively expensive maintenance schedules could maintain the design conditions.

In view of these considerations, it is concluded from the temperature-expansion relations shown by the curves of Figure 9 that one millimeter of longitudinal expansion observable in the manholes per degree centigrade sheath temperature rise is a practical design factor. The length of cable section is not an important consideration, because all sections in normal operation are reasonably close to the average length of the six sections on these tests. In addition, the realized longitudinal expansion per unit length of cable is less for a long section than for a short section, and this tends to make the length of the section a rather unimportant factor in determining the anticipated longitudinal expansion.

On the system under test, two cables were carried by the same 17-inch arms in the manholes. This arrangement will accommodate about 50 millimeters of total section expansion or about 25 millimeters at each duct mouth. The 50-millimeter expansion produces about five inches of lateral movement of the splices. If there is a difference in the loading of the two cables on the same arm, the permissible lateral motion may be reduced.

The 50-millimeter limitation for total longitudinal expansion is acceptable only where the cables are properly arranged in the manholes and where the radii of curvature at the duct mouths and at the arms are appropriate. The life of the cable sheath under the flexing produced by the proposed expansion limit is not known accurately, but our interpretation of the tests reported to the AIEE last year by E. W. Osterreich is that at least 20 years of operation can be expected before sheath trouble due to expansion and contraction becomes serious. Occasional operation at higher temperatures and greater expansions should not effect the life of the sheath but may cause incidental difficulties.

The degree to which the results of these tests will apply to other installations is not known, but it is probable that the reaction of installations involving other sizes and types of cables and ducts will vary considerably from the findings given here. It is apparent, however, from these tests that the longitudinal expansion of the cable as now installed is a more serious limitation than the thermal limitations of the insulation for load cycles such as those experienced in supplying residential load.

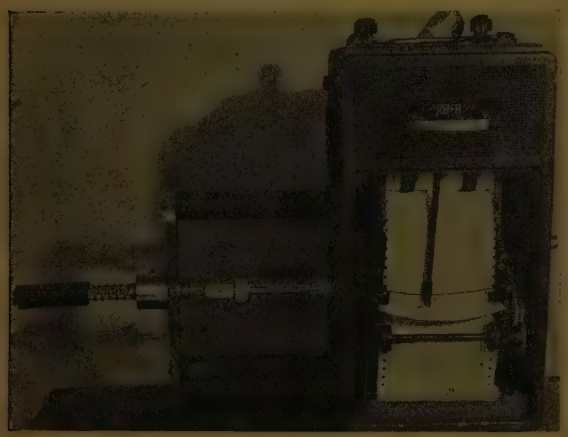


Figure 3. Expansion-recorder mechanism

The flexible shaft transmits cable motion to the recorder pen



Figure 4. Superimposed pictures to show cable motion in a manhole

The dashed lines outline the positions of the expanded cables under heavy load. The solid lines outline the contracted positions of the cables under no load

Mechanism of Expansion

The tests described in this paper were made to determine the safe limits for the loading of a specific system. However, it is felt that so much improvement can be made in the methods of constructing and installing cable as a result of a more complete understanding of the mechanism of cable expansion under load that we have examined these test results for data



Figure 5. Manhole camera installation

Care in positioning camera in manhole opening permits making a graphic record of cable motion by superimposed pictures as shown in Figure 4

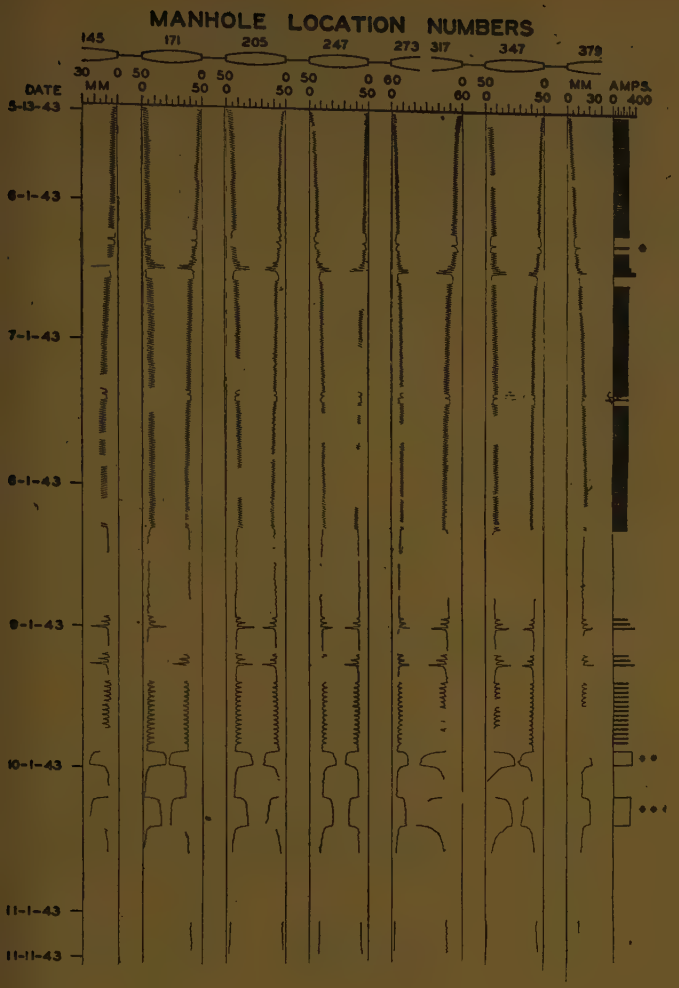


Figure 6. Continuous record of cable expansion

Motion recorded at the 12 ends of the six test sections is plotted against load cycle and date. The scale of motion at the top of the figure is in millimeters. The load cycles are of four-hour duration except those marked *, **, and ***, which are 7, 72, and 149 hours duration, respectively.

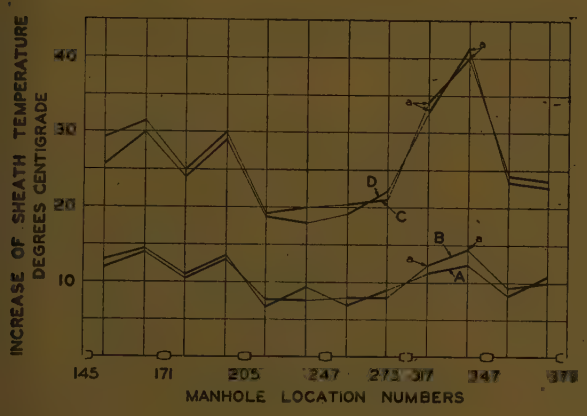


Figure 7. Temperature rise of cable sheath

The heavy, light, and dashed lines connect points plotted for temperatures measured at the ends of the same section, adjacent sections, or sections not adjacent, respectively. For test conditions see Table I

that might be helpful in this understanding. We believe the following observations warrant further consideration:

1. The amount of longitudinal expansion appearing in the manholes for a given increase in cable temperature due to losses in the conductors was different for tests made at different periods of the year.

2. The average longitudinal expansion appearing in the manholes per degree of increase in cable temperature was greater for the larger increases of cable temperature.

3. The amount of longitudinal expansion appearing in the manholes was only about one half of the theoretical free longitudinal expansion of either the copper conductors or the lead sheath (see Figure 10).

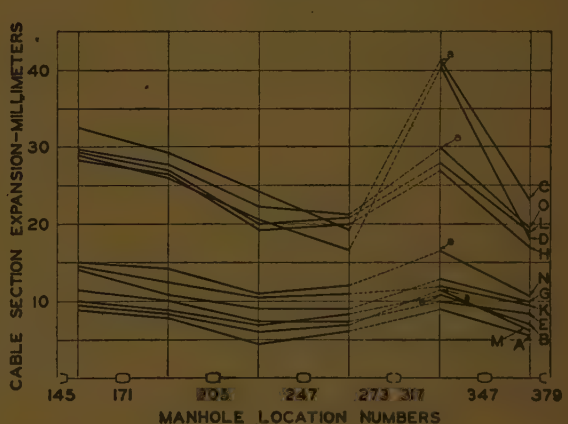


Figure 8. Cable expansion appearing in manholes

The solid and dashed lines connect points plotted for adjacent sections and for sections which are not adjacent, respectively. For test conditions see Table I

The following is a theoretical analysis of cable movement that explains these three observations. The theory is offered with the knowledge that there are insufficient test data to support it but with the hope that it may stimulate further investigation.

Consider first an idle cable in a conduit line which carries no other cables. At the time of lowest earth temperature, the cable is contracted to its minimum length and is in a straight path along the bottom of the duct. As the earth temperature and consequently the cable temperature increases, the cable expands longitudinally. The initial increase in the length of the cable is taken up principally by lateral displacement or "snaking" of the cable within the duct, since the resistance to this "snaking" is less than the resistance to longitudinal motion. Some longitudinal motion is present, and some of the increase in length appears in the manholes, in spite of the freedom of the cable to reposition itself within the duct.

The longitudinal expansion probably is due to the greater thermal expansion of the lead sheath than of the copper conductors. This undoubtedly produces a longitudinal mechanical force in the sheath, part of which is transmitted to the conductors through the insulation, so that it acts to increase the length of the entire assembly. This increase in length of the entire assembly is readily taken up as lateral displacement of the cable within the duct. However, some of the stress in the sheath must be transmitted along the sheath and is effective only as a force to increase the length of the cable into the manholes. The ability of the sheath to produce such movement is limited by the elastic deformation of the sheath and the maximum compressive force which the sheath can transmit longitudinally without permanent deformation.

At the time of maximum earth temperature, which may represent a rise of as much as 25 degrees centigrade above minimum temperature, the cable is expanded sufficiently so that the resistance to added lateral deflection or "snaking" within the duct is considerably increased and the over-all tendency for expansion is into the manholes.

Next, assume that the cable under consideration is carrying load. The action of the forces produced by the temperature changes caused by the electrical losses must be analyzed in relation to the effects of ambient temperature which were discussed in previous paragraphs. For example, if a moderate load is placed on the cable at the time of lowest earth temperature, the longitudinal expansion starts with the cable flat in the duct. Under this condition, a large part of the increase in length is readily absorbed by lateral movement within the duct, and, for a given temperature rise, the expansion appearing in the manholes will be a minimum. However, if the same loading is placed on the cable at the time of maximum earth temperature, the increase in cable temperature is the same as before, but the amount of expansion of the cable into the manhole is from 20 to 50 per cent greater. The reason for this, of course, is that most of the lateral freedom of the cable within the duct was absorbed by the expansion due to earth temperature increases.

The mechanism of cable movement is complicated by other variables. For example, the temperature at which an unloaded cable will assume a straight line on the bottom of the duct undoubtedly depends on previous loadings. If the cable experiences a relatively severe load cycle so that the space available for lateral displacement within the duct is fully occupied before the top of the cycle, then it would appear reasonable that the cable on cooling would tend to straighten out in the duct before withdrawing the cable which had expanded into the manholes. This would have the effect of raising the temperature level at which the cable would be in a straight line in the duct.

The theory outlined in the preceding paragraphs not only explains the performances observed during the tests reported in this paper, but also accounts for a type of damage that occurs to the lead sheath of cables in operation. If the lead sheath is reasonably free to move with respect to the core of the cable, then the forces of longitudinal expansion of the sheath are in part transmitted into that portion of the sheath in the manhole. These forces are maximum where the movement of the sheath is restrained as at a bend or at a solid-filled splice or at the location where the equal opposing forces of an adjacent section appear. Frequently this results in the lead sheath exceeding its elastic limit at one of these points, and a wrinkle or ripple appears in the sheath all around the cable. Once this ripple develops, all further expansion of the sheath is concentrated at that ripple, and failures do not appear at other locations on the sheath. This type of failure is very commonly found at the ends of splices, particularly on those installations where the cables are installed without bends.

If the lead sheath fits rather tightly on the core of the cable, the longitudinal forces developed between core and sheath due to excess expansion of the sheath are continuously transmitted to the core, and the action of the sheath in straightening the cable is reduced or possibly eliminated.

This suggests the desirability of investigating experimentally the possible advantages of improving the mechanical bond between the core and the sheath of the cable. It is possible that the use of properly shaped core-binder tapes and the use of sheath reinforcement would provide sufficient bond between the sheath and the core to insure greater lateral expansion within the duct and less expansion into the manhole. It may even be within the realm of possibility that a carefully co-ordinated design would make possible the elimination of all expansion loops in a cable installation. If would, of course, be necessary to restrain the movement of such a cable into the manhole, perhaps by a suitable enclosure for the cable within the manhole. If a cable so installed had its sheath and core mechanically interlocked, we believe that the entire expansion would be taken up as lateral deformation within the duct, and, if the operating temperature increases became high enough to cause the sheath stress to exceed the elastic limit, the permanent deformation would be spread over the entire length of cable, and there would not be localized portions of the sheath stressed to the point of failure.

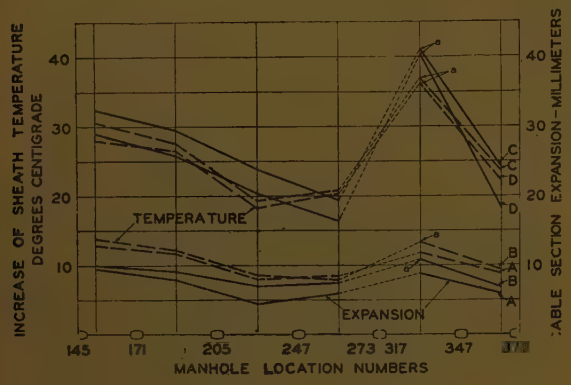


Figure 9. Sheath temperature rise and associated cable expansion appearing in manholes

The solid lines represent expansion. The heavy dashed lines represent temperature rise. The light dashed lines connect points plotted for sections which are not adjacent. For test conditions see Table I

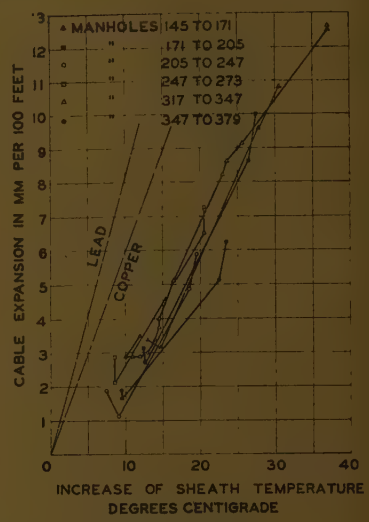


Figure 10. Relation between increase in cable-sheath temperature and realized unit cable expansion

The solid lines connect points relating to individual sections. The dashed lines indicate the friction-free temperature–expansion characteristics of lead and copper assuming a temperature differential between the two metals approximating the differential found in a loaded cable

The Summation of Load Curves

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MEMBER AIEE

A NUMBER of load curves may be added to determine some characteristics of a composite load from known values of the individual loads which comprise it. The resulting peak load, minimum load, load factor, or time of the peak are characteristics usually sought, but there may be many others.

Operators seek to maintain high load factor in a power system in given areas, in feeders, and in equipment to obtain maximum use of the facilities, and to this end loads are combined, so far as practical limitations will permit, for optimum results, which are generally measured finally by return on the plant investment. Hence the subject of rates is linked to that of load curves.

For instance, the economic justification for central-station generation as compared to local generation near individual loads, depends primarily on two facts:

1. The unit kilowatt-hour cost of energy delivered has, in general, been lessened as the amount generated has increased.

2. Relative generating capacity, feeder capacity, and substation capacity required have decreased as additional loads have been added and the demand factor has decreased due to the effects of diversity, through the serving of many loads of greatly diverse character, resulting in improved load factor or use of facilities.

The advantage for central-station generation offered by condition 2 is pertinent to the subject matter of this paper. It may be partially neutralized by adding certain types of loads in excessive amounts. The principles involved will be recognized by engineers who are striving to keep their organizations competitive and build up their revenues at the same time.

Also, in wartime, when old economic controls no longer govern, the need for intelligent load forecasting is apparent to all. It is realized now that in the haste to arm and produce, after a prolonged period of comparative business stagnation, there was much overestimation of loads, with corresponding delays because of some demands for equipment not really needed.

Forecasting under war conditions becomes difficult for load characteristics and timing have changed. Factors based upon past observations are no longer reliable. Industries that formerly operated on a single shift may now be on a 24-hour day, seven-day week basis. Wartime has changed the characteristics of lighting loads, while shortages of manpower and materials have had their effect upon commercial loads. Many utility companies have had their peak-load periods displaced as much as 12 hours under war conditions.

It is prudent to examine trends from time to time, and in so doing, one is confronted with the need to determine the components which comprise a load or conversely to determine the result of combining loads. The purpose of this paper is to consider some of the technical aspects of these problems and to present some methods which, it is hoped, will be helpful.

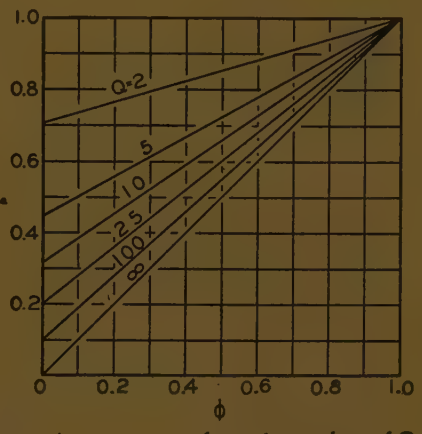


Figure 1. Δ_Q versus ϕ for various values of Q

$\alpha = 2$ and $p = 1.00$

The Addition of Loads Which Are Similar Except for Timing

An understanding of the case in which a number of loads Q, of equal load-factor ϕ, are to be combined, is helpful in determining the methods to be applied to the more general case for loads of various load factors.

In a previous paper[1] load-curve summation was mentioned briefly and the formula presented:

$$\phi_Q = \left[1 + \left(\frac{1}{\phi} - 1\right) Q^{-1/\alpha}\right]^{-1} \qquad (1)$$

in which ϕ_Q was given as the load-factor for the sum of Q equal loads of load-factor ϕ each and α was stated to be a variable of value two or greater.

A more general form of equation 1 is given by:

$$\phi_Q = \left[a + \left(\frac{1}{\phi} - a\right) Q^{-1/\alpha}\right]^{-1} \qquad (1a)$$

where a is the asymptote of the curve of $1/\phi_Q$ versus Q, instead of unity as is the asymptote given in equation 1.

In previous applications of the formula, α was taken as greater than two without introducing a value for a when adjusting for peak concentration. This procedure is not rigorous and does not give true results with any constant α for large range of values of Q and periods of peak concentration because the asymptote is variable and greater than unity when the peaks are concentrated within periods less than that for which ϕ has been calculated.[1]

A preferable approach is to find the value of the asymptote a. With α taken as a constant equal to two, a then becomes equal to z/ϕ, in which z is the coincidence factor for $Q = \infty$ as will be seen subsequently.

Further investigation has convinced the author that for utility services, the number of hours, on the average, during which peaks of a given load factor occur, decreases as the load factor decreases in accordance with probability laws which can be expressed analytically.

Let us examine the meaning of ϕ itself. Suppose a constant load lasts for a period of one hour and no longer. On this one-hour basis the load factor is 1.00; on an eight-hour basis it is 0.125; and on a 24-hour basis only 0.0417; all for the same load. So load factor must always be associated with the time for which it is measured. Also, a time restriction in the occurrence of the peaks alters Δ_Q by changing α or ϕ as given in equation 2, which, for $\alpha = 2$, applies to a random peak-time distribution during the time for which ϕ is considered. If peaks of one-hour duration should all occur during the same hour, they would be added arithmetically. This is one extreme condition. The other extreme condition would occur when the peaks are evenly distributed throughout the time considered.

Since the coincidence factor Δ_Q is the ratio of the peak for Q loads to Q times the peak for one load (the inverse of the diversity factor), we can write, taking the peak for one load as unity:

$$\Delta_Q = \frac{Q\phi}{\phi_Q} \div Q = \frac{\phi}{\phi_Q}$$

so that

$$\Delta_Q = a\phi + (1 - a\phi) Q^{-1/\alpha} \qquad (2)$$

substituting the value of ϕ_Q given in equation 1a. Values of Δ_Q versus ϕ for various numbers of loads are shown on Figure 1 with α taken equal to two and a equal to one. As Q becomes infinitely large $\Delta_Q \doteq \phi$. Also note that as $\phi \doteq 0$, $\Delta_Q \doteq Q^{-1/2}$.

Paper 44-98, recommended by the AIEE committee on power generation for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted December 3, 1943; made available for printing May 5, 1944.

R. F. HAMILTON was with the War Production Board, Washington, D. C., at the time this paper was prepared; he is now with the Foreign Economic Administration, Washington.

Refer to Figure 2. Assume five equal momentary peak loads A, B, C, D, and E, to be concentrated and evenly distributed within a time p. The momentary peak, at the time of the peak C, of the sum of these five loads will be the sum of the five ordinates F, G, C, H, and I, spaced equally in time on the curve FCI. J, K, L, M, and N, are relative instantaneous peak values of the sum of the five loads at the times corresponding to peaks A, B, C, D, and E. Note that L is the highest of these values corresponding to the time of the peak C of the curve which is symmetrical within the time p.

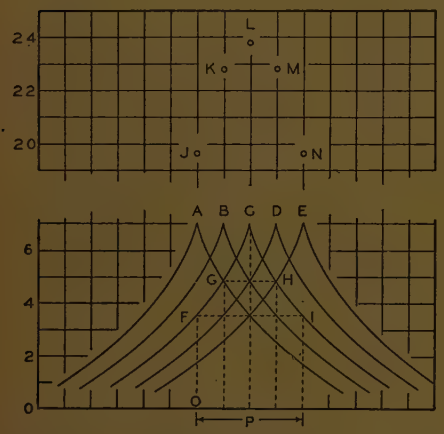


Figure 2. Five equal peak loads occurring within a time p

Now as the number of loads is increased above five, the momentary peak L of their sum will approach as a limit the integral of the curve FCI over the interval of time p. For uniform distribution with reference to time one can therefore write for equal loads:

$$M_Q \doteq Q \cdot p^{-1} \int^{p} \left(\frac{m}{M}\right) \alpha \left(\frac{t}{T}\right) \doteq Qz \qquad (3)$$

for $Q \doteq \infty$, measuring time from the point O of Figure 2.

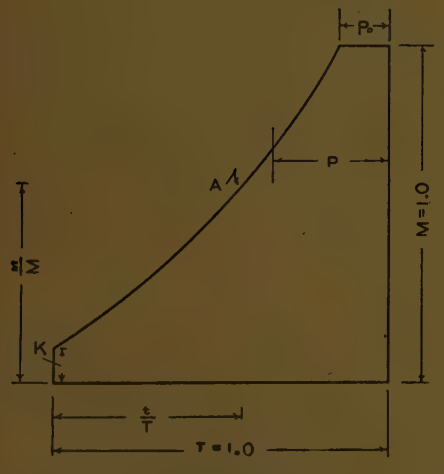


Figure 3. Reversed time-duration curve M, T, ϕ, k, and p_0 given

This equation simply states that the peak value for $Q \doteq \infty$ loads approaches Q times the average value for one symmetrical load during the interval p. This average value we have called z. For non-uniform distribution during the time p, the peak M_Q, following the most probable random distribution, will exceed Qz by an amount equal to $(1\text{-}z)Q^{1/2}$. This follows from equation 1 using $\alpha=2$ and replacing ϕ with z, or

$$M_Q = Qz + (1-z)Q^{+1/2} \qquad (4)$$

$$\Delta_Q = z + (1-z)Q^{-1/2} \qquad (5)$$

Also from equation 2 it is apparent that $a = z/\phi$ if α is to be two and equation 5 holds true.

Suppose we now set up a hypothetical load synthetically, taking into consideration minimum value, maximum value, duration of the peak, and load factor, using the methods heretofore described[1], in accordance with Figure 3:

ϕ is the load factor.
k is the variation factor.
p_0 is the peak duration.
T, the total time, is unity.
M, the maximum value, is unity.

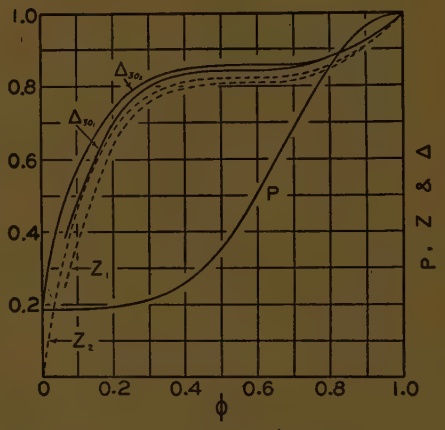


Figure 4. p, z, and Δ_{30} versus ϕ using equations 8 and 9 for z_1 and z_2

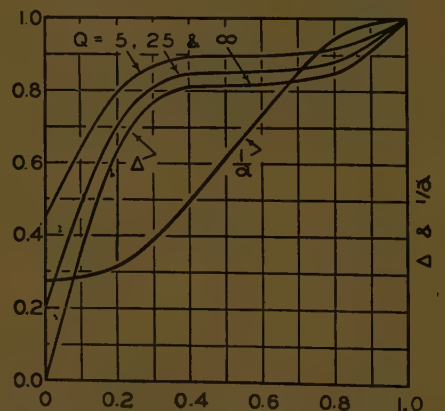


Figure 5. Curves of 1/a versus ϕ and Δ_Q versus ϕ for various values of Q

The curve A can be expressed as:

$$m/M = k + (1-k)(1-p_0)^{-n} \cdot \left(\frac{t}{T}\right)^n$$

where

$$n = \frac{(1-\phi)}{(\phi-k)-p_0(1-k)} \qquad (6)$$

This will represent a load-duration curve with abscissas reversed in sign.

Let p be a period of time which includes the period of the peak p_0, and $(p - p_0)$ in addition. By integrating this load-duration curve over the time p and dividing the result by p, one obtains z, the average value, and hence the load factor for that portion of the load included within the time p.

$$z = k + p^{-1}\{(\phi-k) - [(\phi-k) - p_0(1-k)] \times \left[\frac{1-p}{1-p_0}\right]^v\} \qquad (7)$$

where

$$v = \frac{(1-k)(1-p_0)}{(\phi-k)-p_0(1-k)}$$

For a one-hour peak in a 24-hour cycle, $p_0 = 1/24$; also in utility curves it has been

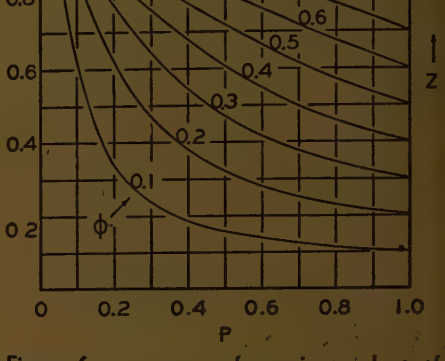


Figure 6. z versus p for various values of ϕ according to equation 8

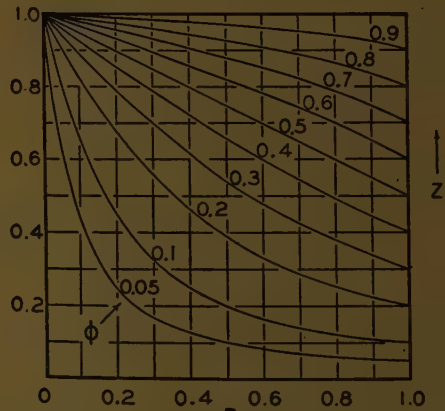


Figure 7. z versus p for various values of ϕ according to equation 9

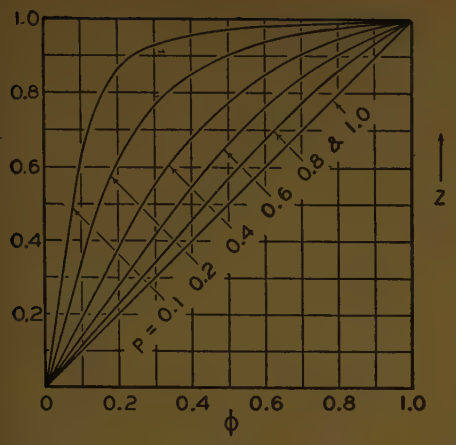


Figure 8. z versus ϕ for various values of p according to equation 8

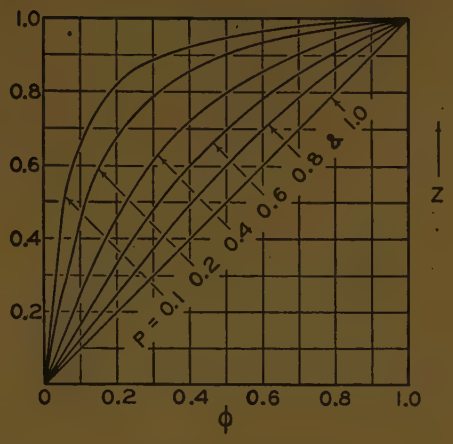


Figure 9. z versus ϕ for various values of p according to equation 9



Figure 10. m/M versus hours of peak displacement for various values of ϕ using equation 12

$k = 0.4\,\phi$ and $p_0 = 1/24$

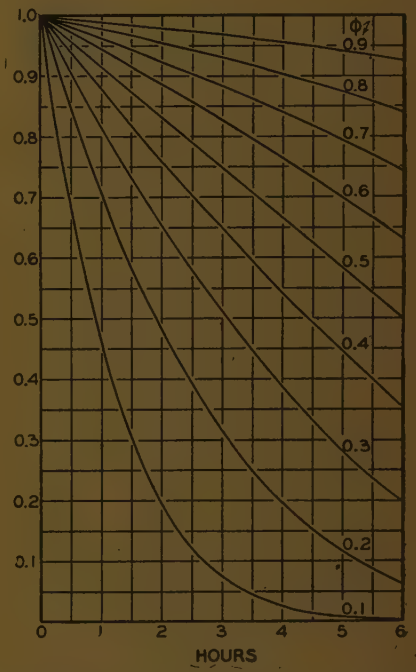


Figure 11. m/M versus hours of peak displacement for various values of ϕ using equation 12

$k = 0$ and $p_0 = 0$

found that one can use $k = 0.4\phi$ usually. Under these conditions:

$$z=\frac{1}{10}\left[4\phi+p^{-1}\left\{6\phi-\frac{1}{12}\,[74\phi-5]\times\left[\frac{24}{23}(1-p)\right]^{\frac{23(5-2\phi)}{74\phi-5}}\right\}\right] \quad (8)$$

For momentary peaks or for weekly and monthly load-factors for which p_0 becomes very small and where k approaches zero in value, one can put p_0 and k equal to 0 in equation 6 and then:

$$z=\frac{\phi}{p}\{1-[1-p]^{1/\phi}\} \quad (9)$$

The purpose of equations 7, 8, and 9 will be obvious. If the peaks of a number of loads Q, of equal load factor ϕ, are restricted to a time p, which is less than the time for which ϕ is calculated, then we shall use z instead of ϕ. If our cycle is one of 24 hours, then $24p$ will be the number of hours in which the peaks are concentrated.

Probability Elements as Applied to Consumers' Habits

Consider the not unusual custom of measuring all consumers' maximum demands on a unit-time basis. If these demands in kilowatts are also compared on a unit basis regardless of magnitude then, relatively, a consumer's load factor is a direct measure of his energy consumption throughout the day, and it seems logical to presume that the more energy he consumes the more likely is the time of his peak-load to vary. Stated in another form, when a substantial number of customers with about the same load factor have been segregated, it is found that the time in which the peaks occur can be correlated with the load factor. Such a correlation may be expressed:

$$p=b+ce^{-h^2(f\phi)^2} \quad (10)$$

to conform to Gauss' normal probability curve.

A study of Constantine Bary's results covering large and small light and power, commercial and industrial consumers in Philadelphia, Pa., would indicate values for b, c, h^2 and $(f\phi)^2$ in equation 10 as follows[2] (see figure 4):

$$p=0.187+0.813e^{-4\{(1-\phi)^2+(1-\phi)^{2\cdot8}\}} \quad (10a)$$

Other values of these quantities will be found for other special types of load and may be expected, it is believed, for the same types in other locations.

The curve of $\phi/z=1/a$ versus ϕ also assumes the shape of a probability curve as can be seen in Figure 5, and may be expressed empirically without reference to p and z. However, for momentary peak loads and with p known, a would be given by, (p_0 and $k = 0$):

$$a=\frac{1}{p}\{1-(1-p)^{1/\phi}\} \quad (11)$$

from equation 9 and likewise other values from equations 7 and 8 for the conditions therein given for p_0 and k. The quantity a is the value of the asymptote of equation 1a as previously discussed. Figure 5 also shows the variation of Δ_Q with Q, for the values of $1/a$ on the curve.

The Effect of Peak Time Concentration on the Coincidence Factor

Refer to Figure 4. The curve marked p represents values of p as calculated from equation 10a and $24p$ is the equivalent hours per day in which the total number of peak loads may be considered as concentrated. (See note following example 5 in the Appendix.)

The curve marked z_1 represents values of z calculated from equation 8 using the corresponding values for p just found.

The curve marked z_2 represents values of z calculated from equation 9 using the corresponding values of p. For z_1, p_0 is 1/24 (one-hour peak) and $k=0.4\phi$ and for z_2, p_0 is 0 (instantaneous peak) and $k=0$, as explained previously.

Note that z_1 and z_2 apply to an infinite number of loads. By concentrating the time of the peaks in accordance with curve p, we have shifted the straight diagonal line of Figure 1, $\Delta_Q \doteq \phi$, for an infinite number of cases, to one of the peculiarly shaped curves $\Delta_Q \doteq z$ of Figure 4. See also Figures 8 and 9.

Now by applying equation 5 for the summation of 30 equal loads, we obtain the two curves of Figure 4 shown for Δ_{30}

Tests conducted by Constantine Bary[2], for 30 combined loads and analyzed subsequently by H. E. Eisenmenger[3], and others, have given results that agree closely with these curves, except that Bary's curve for 30 combined loads terminates at 0 for $\phi \doteq 0$, whereas that of the author terminates at 0.1825 for $\phi \doteq 0$, for Δ_{302}. In the case for ϕ_{301}, in which the peak is measured on a one-hour basis, a load factor less than 0.0675 is impossible, for p_0 has been taken at 1/24 and $k = 0.4\phi$.

The value $k = 0.4\phi$ seems to apply closely enough for utility loads. It is not

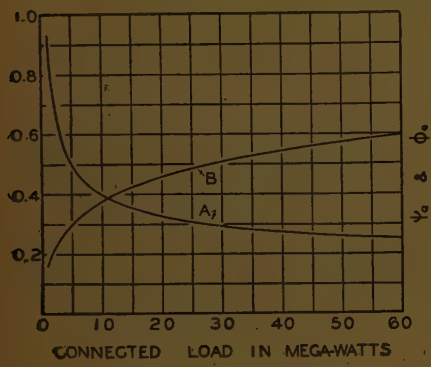


Figure 12. Demand factor and load factor versus connected load for military establishments

critical. For example, with $p_0 = 1/24$, $\phi = 0.5$, and k taken as 0.2ϕ, 0.3ϕ, 0.4ϕ, and 0.5ϕ, respectively, the corresponding values for z using equation 7 would be 0.803, 0.792, 0.775, and 0.757, for $p = 0.4$.

In this case, a 60 per cent decrease in the value of k from 0.5ϕ results in about six per cent increase in the value of z. Of interest too is the fact that a given percentage change in p usually causes less than one-half this percentage change in z.

Adding Loads of Various Load Factors

Equation 6, or others similar, which give consideration to the load-curve shape, can be used to find the value of a given load at any time p from the peak. Otherwise stated, if the time of a peak-load is known, the effect of this peak at any other time as added to any other peak or peaks can be estimated closely. This method is specifically applicable for a substantial number of peaks when their hour-to-hour characteristics are not readily obtainable, or where these vary and average values will suffice. It is not offered for a few curves as a substitute for the actual load curves if these are available. The curve for m/M, in equation 6, and others similar, is for a load duration curve with abscissas reversed. When used for direct-time readings of m/M, zero time may be taken at the midpoint of the peak load ($1/2\ p_0$) and the value of m/M would then be given by:

$$\frac{m}{M} = k + (1-k)\left[\frac{1-2p}{1-p_0}\right]^n$$

where

$$n = \frac{(1-\phi)}{(\phi-k)-p_0(1-k)} \qquad (12)$$

Note that $2p$ is used instead of p when the equation is applied in this manner.

For Q loads occurring with a random distribution and having various load factors, the peak value of the sum of the loads may be found from:

$$M_Q = \Sigma(M\phi) + \left(\frac{1}{Q}\Sigma[M^2(1-\phi)^2]\right)^{1/2} \qquad (13)$$

$$M_Q = \Sigma(Mz) + \left(\frac{1}{Q}\Sigma[M^2(1-z)^2]\right)^{1/2} \qquad (14)$$

depending upon whether the peaks are distributed throughout the cycle of ϕ or concentrated during p so that z becomes applicable as heretofore discussed. These follow from equations 2 and 4.

The corresponding coincidence factors Δ_Q may be obtained from equations 13 or 14 by dividing M_Q by ΣM.

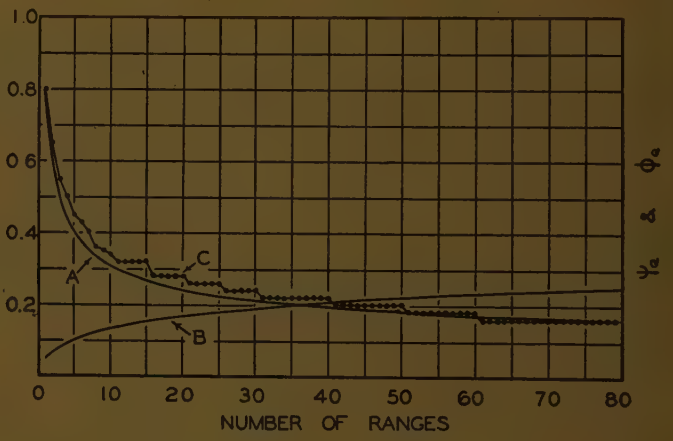


Figure 13. Demand factor and load factor versus number of electric ranges

$p = 0.5$

The Effect of Load Changes on Coincidence Factors and System Peaks

Coincidence factors have been employed to estimate the effect of an additional load on an existing peak load. It will be seen that this method can be applied only when the existing peak occurs during a time in which the additional load is apt to occur. If the time of a principal peak load has shifted, the relations which determine the relative contributions to this peak load are certainly altered. A single industrial customer, in the change from 8-hour to 16- or 24-hour operation, has correspondingly increased his load factor and, therefore, the hours during which his maximum demand (which may have remained constant) is likely to occur. The effect of a substantial number of such load changes would not necessarily increase a system peak. It might conceivably reduce it, at least alter its timing. Failure to understand or evaluate this Δ versus ϕ relationship and to foresee the limitations in materials and manpower, may in part account for many of the extremely high estimates which have been made from time to time of the total generating capacity needed for the war effort.

The solutions which are based upon probability relationships increase in accuracy as the number of loads increases. For a few loads, one may speak of the most probable solution. For a substantial number of loads, one may with more confidence speak of the actual solution for some of these intangible problems.

Demand Factors for Composite Loads

Since, for unit loads of all load factors, the load factor of the summation of a substantial number of such loads increases as the number of loads increases, it is apparent that the demand factor will decrease for given types of loads as the connected load increases. If the demand factor for Q loads is ψ_Q and the connected load is β_Q

$$\psi_Q = \frac{M_Q}{\beta_Q} = \frac{\Delta_Q \Sigma M}{\Sigma \beta} = \frac{\phi_1 Q M_1}{\phi_Q Q \beta_1}$$

$$= \frac{1}{\phi_Q}[\phi_1\psi_1] = \Delta_Q\psi_1 \qquad (15)$$

Thus, for loads of a given load-factor type ϕ_1, ψ_Q is proportional to $1/\phi_Q$, since $[\phi_1\psi_1]$ is a constant value for that type of load. M_1/β_1 represents the kilowatt demand per kilowatt of connected load for the unit load, and, therefore, $[\phi_1,\psi_1]$ is the average load per kilowatt of connected load.

It has been determined that by dividing a given type of connected load into Q hypothetical unit blocks of 10, 100, or 1,000 kw each, useful empirical relations may be established for the demand-factors.

For example, for military establish-

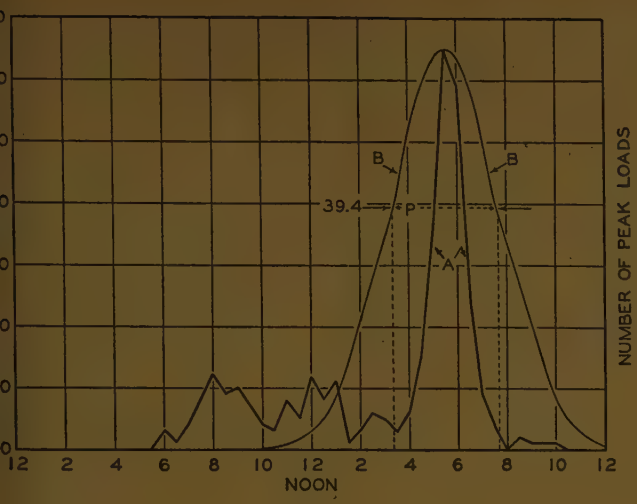


Figure 14. Frequency curve for individual electric-range peak loads

nents of certain kinds ranging in magnitude of connected load from 5,000 to 50,000 kw, the demands have been found to fall within or near the values given by the above relations with $p=1.0$, $\phi_1=0.159$, and $\psi_1=0.935$ for 1,000-kw unit blocks of connected load as given on curve A of Figure 12. Curve B of the same figure is for the corresponding load factors.[4]

Another example, for domestic electric ranges, is illustrated by curve A of figure 13 which represents demand factors versus number of ranges for $p=0.5$, $\phi_1=0.05$, $z=0.10$ (equation 9), and $\psi_1=0.8$. Curve B is for the corresponding load factors and curve C gives values of demand factors as given in the National Electric Code.[5]

Conclusion

The more one delves into this subject of load-curve analysis, the more apparent does it become that here is to be found an opportunity for research, particularly by utility companies, which should be profitable. It is hoped that in this brief paper, a few load-curve relations have been presented in a manner that will point to the possibilities for further investigations.

To explain more fully practical applications of some of the formulas presented, additional figures and a few examples are included in the Appendix.

Appendix

Symbols Employed in the Paper

ϕ—Load factor
Δ—Coincidence-factor
p—Peak-concentration factor (or peak-shift factor)
p_0—Peak-duration factor
k—Load-variation factor
z—Coincidence-factor for an infinite number of similar loads
Q—The number of loads
Δ_Q—Coincidence-factor for Q loads
M—Maximum value of a load
M_Q—Maximum value for the sum of Q loads
ψ—Demand factor
β—Connected load
α—An empirical number, value two or more
Σ—Sign of summation for terms following
T—Time of load cycle
t/T—Fraction of load cycle (in time)
m/M—Fraction of peak load (in power)

Explanation of Additional Figures

Figure 6 shows the z versus p relationship for various values of ϕ as calculated from equation 8, in which $k=0.4\phi$ and $p_0=1/24$.

Figure 7 shows the z versus p relationship for various values of ϕ, as calculated from equation 9, in which $k=0$ and $p_0=0$.

Figure 8 corresponds to Figure 6 except that the relationship of z versus ϕ of equation 8 is given for various values of p.

Figure 9 corresponds to Figure 7 except that the relationship of z versus ϕ of equation 9 is given for various values of p.

Figure 10 gives values of m/M versus hours of peak displacement for various values of ϕ as calculated from equation 12 with $k=0.4\phi$ and $p_0=1/24$.

Figure 11 gives values of m/M versus hours of peak displacement for various values of ϕ as calculated from equation 12 with $k=0$ and $p_0=0$.

Example 1

The ten loads shown in Table I are to be combined on a single circuit. What peak load may be expected at 6:00 p.m. and at 9:00 p.m.? Refer to equation 12 and Table II.

Example 2

What peak load may be expected on a power feeder which is to carry the loads shown in Table III? Peaks are on a 15-minute basis. In this case $p_0=1/96$. Take $k=0.4\phi$ and apply equations 7 and 14. See Table IV.

$$M_Q=3{,}436+\left(\frac{1{,}020{,}444}{10}\right)^{1/2}=3{,}755 \text{ kw}$$

$$\Delta_Q=\frac{3{,}755}{5{,}600}=0.67$$

(Note: average $z=0.633$)

Table I

Number	Kw Maximum	Kw Minimum	Time of Peak, P.M.	Peak Duration Minutes	Kilowatt-Hours Per Day
1	10,000	500	6:00	30	48,000
2	2,000	100	5:30	60	12,000
3	2,500	250	8:00	15	24,000
4	5,000	500	4:15	5	60,000
5	1,000	80	7:20	30	8,400
6	4,500	900	9:00	60	64,800
7	1,800	630	4:30	15	34,560
8	900	18	3:30	15	4,320
9	500	60	7:30	60	3,600
10	1,000	150	4:45	20	3,600
	29,200				

Table II

Number	ϕ	k	p_0	p, 6 P.M.	p, 9 P.M.	M, 6 P.M.	M, 9 P.M
1	0.20	0.05	0.0208	0.0000	0.1250	10,000	2,342
2	0.25	0.05	0.0417	0.0208	0.1458	2,000	611
3	0.40	0.1	0.0104	0.0833	0.0417	1,830	2,169
4	0.50	0.1	0.0035	0.0729	0.1979	4,204	2,859
5	0.35	0.08	0.0208	0.0556	0.0694	796	739
6	0.60	0.2	0.0417	0.1250	0.0000	3,636	4,500
7	0.80	0.35	0.0104	0.0625	0.1875	1,737	1,580
8	0.20	0.02	0.0104	0.1042	0.2292	327	69
9	0.30	0.12	0.0417	0.0625	0.0625	344	344
10	0.15	0.04	0.0139	0.0521	0.1771	410	63
						25,283 kw	15,276 kw

Peak load at 6:00 p.m. 25,283 kw
Peak load at 9:00 p.m. 15,276 kw

Table III

Number	Kilowatt-Hours Per Day	Kw Peak	Hours in Which Peak May Occur
1	1,200	100	9:00 a.m. to 9:00 p.m.
2	1,800	300	9:00 a.m. to 7:00 p.m.
3	8,640	600	8:00 a.m. to 8:00 p.m.
4	1,440	200	10:00 a.m. to 10:00 p.m.
5	6,000	1,000	4:00 p.m. to 10:00 p.m.
6	9,600	2,000	5:00 p.m. to 10:00 p.m.
7	13,440	700	12:00 m. to 12:00 m.
8	1,080	50	12:00 m. to 12:00 m.
9	1,440	400	5:00 p.m. to 11:00 p.m.
10	600	250	4:00 p.m. to 8:00 p.m.
	42,240	5,600	

Table IV

Number	ϕ	p	z	Mz	$M^2(1-z)^2$
1	0.5	0.500	0.699	70	906
2	0.4	0.417	0.665	200	12,800
3	0.6	0.500	0.781	469	17,266
4	0.3	0.500	0.468	94	11,321
5	0.25	0.250	0.592	592	166,464
6	0.2	0.208	0.563	1,126	763,876
7	0.8	1.000	0.800	560	19,600
8	0.9	1.000	0.900	45	25
9	0.15	0.208	0.458	183	4,700
10	0.10	0.167	0.387	97	23,486
		Average $z=0.633$		3,436	1,020,444

Example 3

Tests made on a few electric ranges of a given type indicate an average maximum demand of 3.5 kw for one range, and a monthly consumption of 125 kilowatt-hours. At 80 per cent transmission efficiency, what will be the increase in a utility company's 7:30 p.m. peak load if the maximum probable use of the ranges occurs at 5:30 p.m. and 10,000 of these ranges are to be placed on the system?

SOLUTION

The load factor for one range $\phi=125$ kilowatt hour per month$-$range/720 hours per month$\times$3.5 kilowatts per range $=$ 0.0496.

The peak-time-concentration factor $p=$ 0.188 (from equation 10).

The coincidence factor for an infinite number of ranges (or the load factor per range for the time-concentration factor p) $=$ 0.26 (from equation 9).

The coincidence factor for 10,000 ranges Q is $\Delta_Q=0.2674$ (from equation 5).

The demand for 10,000 ranges Q is $M_Q=\Delta_Q Q M_1/0.8$ for 80 per cent efficiency. So that $M_Q=(0.2674)(10{,}000)(3.5)/0.8=11{,}699$ kw, 5:30 p.m. range peak.

Now the load-factor for Q ranges, $\phi_Q=\frac{\phi}{\Delta_Q}=0.1865$.

The peak-time-shift factor, $p_s=2$ hours per day/24 hours per day$=1/12$.

The fraction of the range peak at 7:30 p.m. is $m/M=0.447$ (equation 12).

So the range load contribution at 7:30 p.m. is $m/M\times M_Q=5{,}229$ kw.

Example 4

Neglecting transmission losses, what peak demands per heater may be expected for groups of 5, 10, 20, 170, and 500 single-unit domestic-type water heaters of 1,500 watts rating, considering that the maximum use will occur between 7:30 a.m. and 8:30 p.m. and the average monthly consumption per heater is 300 kilowatt-hours?

SOLUTION

The load-factor for one heater $\phi=300$ kilowatt-hours per month per unit/720 hours per month$\times$1.5 kilowatts per unit$=$ 0.278.

The peak-time-concentration factor $p=$ 13 hours per day/24 hours per day$=0.542$.

The coincidence-factor for an infinite number of heaters (or the load-factor per heater for the time-factor p) is

$$z=\frac{\phi}{p}\left\{1-(1-p)^{1/\phi}\right\}=0.482$$

The coincidence-factor for Q heaters Δ_Q $z+(1-z)Q^{-1/2}$.

The demand per heater is $\Delta_Q M_1=1.5$ so that:

Q	Δ_Q	Kilowatts Per Heater
5	0.714	1.071
10	0.646	0.969
20	0.598	0.897
170	0.522	0.783
500	0.505	0.758

The reader is invited to compare the results with Figures 14 and 16 of reference which were based on a large number of test

Example 5

A proposed military post for 40,00 officers and men is expected to have th wintertime loads shown in Table V.

For purposes of determining the capacit of the service feeders and transformers, fin the maximum load, the average load ove a three-hour period, and the rms valu over three hours.

SOLUTION

See Table VI. The quantity p is fro equation 10, and z is from equation 9.

$$M_Q=3{,}477+\left(\frac{464{,}679}{3}\right)^{1/2}=3{,}871 \text{ kw}$$

maximum demand

M_Q is from equation 14.

$$\phi_Q=\frac{33{,}200}{24\times 3{,}871}=0.357$$

For a displacement of $1\frac{1}{2}$ hours from th peak load:

$m/M=0.717=k_2$ (using equation 12 with $k=0.4\phi$ and $p_0=0$

$z_2=0.852$ (using equation 7 with $k=0.4$ and $p_0=0$

z_2 is the load factor over the three-hou period. So that average value over thre hours$=3{,}871\times 0.852=3{,}297$ kw.

Table V

Type	Connected Kilowatts	Daily Use, Kilowatt-Hours	Hours Use of Maximum Demand
1. All lighting	4,100	16,000	8
2. Heating and cooking	4,000	12,000	6
3. Power and miscellaneous	1,500	5,200	10
	9,600	33,200	

Table VI

ϕ	p	z	M	Mz	$M^2(1-z)^2$
1	0.333	0.226	0.791	2,000	1,582	174,724
2	0.250	0.202	0.735	2,000	1,470	280,900
3	0.417	0.273	0.817	520	425	9,055
				3,477		464,679

To find the rms value of load use equation 5 of reference 1. Thus

$$\theta = z^2 + \frac{(z_2 - k_2)^2}{\left(\frac{1-k_2}{1-z_2}\right)^2 - 1} = 0.7945$$

where θ is the loss factor, so that rms value over the three-hour period is $\sqrt{\theta} M = 3{,}450$ kw.

The peak time-concentration factor p, as used in the foregoing example, is an "equivalent time" in which the total number of peak loads may be considered as concentrated.

Actually, in certain types of loads, there may be a frequency distribution of these peak loads which can be represented graphically by a frequency polygon of peak concentration about some given time at which the density of the peak concentration will be a maximum value or mode of the frequency distribution. While not conclusive, it would seem from a limited number of cases that p may be taken as that time (or fraction of the total time of a cycle) which, if squared and multiplied by the total number of peak loads, will give the sum of the second moments about the axis of the mode of the frequency polygon. Abscissas in this polygon would represent times of the class intervals of the peak loads and ordinates would be the numbers of peak loads falling within the various frequency rectangles.

Mathematically, $p \doteq [Q^{-1}\Sigma(p_c Q_c)]^{1/2}$ for the frequency polygon and, if the number of loads becomes large, $p \doteq 1/\sqrt{2}\, h$ where h is the modulus of the probability curve relating p_c and Q_c, thus

$$Q_c = Q_m e^{-(hp_c)^2}$$

and $p \doteq \sigma$, the standard deviation.

Refer to Figure 14 which shows on curve A the peak-load-time distribution for 50 nine-kilowatt electric ranges found in a recent test in Philadelphia over a period of one week, as furnished by Constantine Bary.

Curve B is the probability curve whose standard deviation is equal to the $[Q^{-1}\Sigma(p_c Q_c)]^{1/2}$ of curve A, namely 4.44 hours or, as a fraction of one day, $p = 4.44/24 = 0.185$. Compare this with the value of p in example 3 which was taken from equation 10.

References

1. Synthetic or Equivalent Load Curves, R. F. Hamilton. AIEE Transactions, volume 61, 1942, pages 369–81.

2. Economic Significance of Load Characteristics as Applied to Modern Electric Service, Constantine Bary. *Proceedings*, 53rd Annual Meeting of the Association of Edison Illuminating Companies, January 1938.

3. Study of the Theoretical Relationship Between Load Factor and Diversity Factor, H. E. Eisenmenger. *Proceedings*, 55th Annual Meeting of the Association of Edison Illuminating Companies, January 1940.

4. Estimating Electric Loads for Military-Training Bases, Federal Housing, and Other Wartime Projects, H. M. Potts. Discussion by R. F. Hamilton, AIEE Transactions, volume 62, 1943, pages 463–4.

5. 1940 National Electric Code. Chapter 10, table 29, page 328, Demand-Factors for Household Electric Ranges.

6. Evaluation of Incompletely Diversified Loads, W. L. Tadlock. Electrical Engineering, volume 62, November 1943, pages 485–92.

Electrically Operated Gyroscopic Instruments

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THE topic "Electrically Operated Gyroscopic Instruments" covers a very wide and interesting field in which our work includes, for the most part, instruments and systems closely associated with aircraft, navigation, and control.

Gyroscopic instruments for aircraft originally were air-operated because of the availability of pneumatic pressure from the simple Venturi tube. As the use of electricity aboard airplanes became prevalent, the advantages of electrical operation were applied to the gyroscopic instruments.

The electrically driven gyroscope may be divided into the three commonly used types:

1. The vertical gyroscope.
2. The directional gyroscope.
3. The rate gyroscope.

Since the vertical gyroscope is used for stabilizing the directional reference, the directional gyroscope may be disregarded in this discussion.

Gyroscopes may be classified under the two phenomena associated with rotating bodies:

1. Stability or the fixity of the spin axis in space.
2. Precession or the reaction of the spinning body to a torque about an axis other than the axis of rotation.

Gyroscopes are characterized by the requirements of the maximum moment of inertia about the rotational axis and maximum rotational velocity. The equation $T = I\Omega\omega$ defines the relationship between applied torque, moment of inertia, precessional velocity, and spin velocity in either the vertical or the rate gyroscope. In both types the rotor is designed to provide the largest possible moment of inertia and is constructed, so that it is exterior to the stator. For most purposes the gyroscope is an induction motor designed to operate at 400 cycles from single- or three-phase supply, 110 or 26 volts. Rotor speeds for the large gyro are 10,000 rpm and for the smaller units 20,000 rpm. Power consumption is between four and six watts.

Vertical Gyroscopes

In the vertical gyroscope (see Figure 1) the equation $T = I\,\Omega\omega$ defines the rigidity of the gyroscope. T is an index of the amount of torque which the gyro can resist in the form of friction and disturbing forces. Its importance in the field of aircraft control has been occasioned by the necessity for a fixed reference, regardless of changes in the aircraft's attitude. High velocities, accelerations, pitch, bank, poor visibility, and great altitudes have made it imperative that fixed axes be established to provide reference for control, navigation, bombing, fire control, and so forth. For the purpose of establishing attitude, a stable vertical reference is required. The gyroscope possesses the necessary stability, but it must also be responsive to the earth's gravitational field, so that it may become vertical. A free gyroscope established in the vertical soon would depart from that position because of such factors as precession caused by friction, departure from vertical caused by the earth's rotation, and departure caused by the motion of the craft on the earth's surface. To insure that the gyroscope remains vertical, a device called an erection mechanism (see Figure 2) causes the gyroscope at all times to precess into the vertical axis. The response of the gyroscope and its erection mechanism to the gravitational field cannot be separated from its response to any other acceleration. The problem of seeking the vertical, therefore, is one of separating gravitational acceleration from random accelerations and from those introduced by maneuvers of the craft. Since it is impossible to separate them instantaneously, the combination of the gyroscope and its erection mechanism is so designed that the response of the system to accelerations is very slow. Under these conditions the random accelerations will cancel out over a period of time, while those caused by maneuvers are responded to so gradually, that departure from the vertical, even in extreme cases, is small. Since the gravitational acceleration is constant and continuous, the gyro is at all times corrected to the vertical during normal, straight, and level flight.

Basic Gyro Vertical

We have three gyroscopic elements characterized by different sizes of gyro-rotors. The gyro vertical (see Figure 3)

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consists of an induction motor in which the rotor comprises the gyroscopic mass and is external to the stator to secure maximum moment of inertia. The axis of rotation of the motor is normally vertical. Attached to the housing is the erection mechanism. The rotor, stator, housing, and erection mechanism comprise the gyromotor. This gyromotor assembly is supported on ball-bearing pivots in a gimbal ring, which in turn is supported on ball-bearing pivots in the main housing. The axes of the gimbal support are perpendicular to each other and to the rotor axis. It follows that the two gimbal axes are normally in a horizontal plane. The gyromotor is in a universal joint, and the gyro axis is free in two horizontal axes about its rotational axis. The basic gyro vertical therefore can be described as a gyroscope with three degrees of freedom provided with an erection mechanism.

The erection mechanism (see Figure 2) consists essentially of a steel ball rolling in a circular track. A disk with a slot is mounted above the track allowing the ball to run freely within the limit set for it by the slot. The ball on this track acts as a level and reacts to the inclination of the track, which is perpendicular to the rotor spin axis. A circular magnet attached to the gyrorotor shaft turns inside a drag cup attached to cause the disk to turn. The magnetic coupling is designed so that the disk turns at approximately 30 rpm.

When the gyro axis is vertical the ball travels with uniform velocity, and there are no unbalanced forces. Displacing the gyroscope inclines the track, causing a nonuniform ball velocity due to acceleration. An escapement mechanism, built into the disk, is provided to restrain the speed of the erection system when the gyroscope is in the vertical position and still provide sufficient torque for correct operation of the mechanism when the gyroscope is displaced from the vertical.

The mechanism is so designed and balanced that the rotating mass of the ball, due to its variable velocity, exerts a precessing torque upon the gyroscopic system at an angle of 90 degrees from the inclined angle of the gyroscope to the vertical. The chief consideration is that the variable velocity of the rolling ball creates an erecting torque on the gyroscope which persists until the track is horizontal, at which time the gyroscope is vertical, the ball speed is uniform, and no further unbalanced forces exist. In all three of the gyro verticals power is supplied through the gimbal suspension by means of hairsprings or insulated pivots. The selection of rotor and gimbal bearings is an important problem, since these instruments must function through a temperature range of from −54 degrees centigrade to +70 degrees centigrade; the available torque exerted on the gyroscope is minute and any imperfection in the gimbal bearings will result in failure to attain required accuracy to vertical.

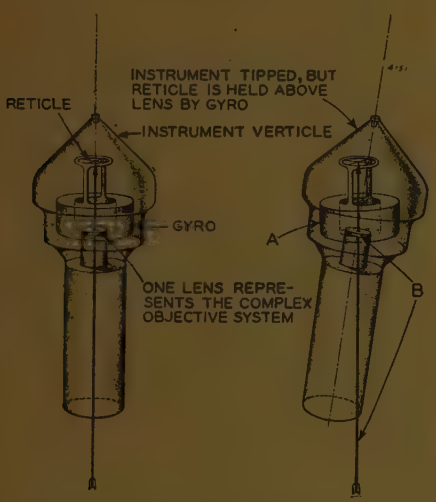


Figure 1. Principle of vertical gyroscope (applied for driftmeter)

A. Schematic gyroscope shown hollow to allow light to pass through, although it is solid in the instrument, and the light is reflected aside by a prism

B. Although the lens is tilted, it still directs the vertical ray to the center of the reticle



Figure 2. Vertical gyroscope showing erection mechanism

Precessional Gyroscope

The characteristics of the precessional gyroscope (see Figure 4) are given again by the equation $T = I\Omega\omega$ in which T is the precessional torque, I is the rotor moment of inertia, Ω is the angular velocity to be measured, and ω is the angular velocity of spin. It is evident from the equation that an angular velocity imposed on a spinning mass will result in a torque which is proportional to that angular velocity. The precessional or rate gyromotor is similar in construction with the gyro vertical in that it consists of an induction motor comprising an internal stator, an external rotor of maximum moment of inertia, a motor housing, and a set of pivots in gimbal bearings attached to the external housing. The basic-rate gyro can be described as a restrained gyroscope with two degrees of freedom. Angular velocity about the reference axis causes a precessional torque about the restrained axis. The torque acts against appropriate springs, causing a displacement proportional to the imposed angular velocity. In a specific case, the rate of turn indicator for aircraft, the rotor axis is in a horizontal plane athwarthship, the gimbal axis is in a horizontal plane along the fore and aft axis. The rate of turn to be measured is about a vertical axis, and the precessional torque is exerted about the fore and aft axis against restraining centralizing springs.



Figure 3. Basic vertical gyroscope (applied for gyro roll indicator)

Production Problems

The imposition of severe restrictions in space and weight on instruments designed for aircraft requires the use of components of small size and extreme precision. For example, decreasing the length of an axis by 50 per cent automatically calls for a 50 per cent reduction in allowable tolerance to maintain the same angular accuracy.

The problems attending the production, inspection, and assembly of small parts of high precision have forced the adoption of methods of manufacture, such as coin-

ing, centrifugal casting, and forging in extreme accuracy and small size. Accuracy requirements have resulted in adoption of production techniques capable of maintaining ±0.0001-inch tolerances. Assembly and calibration of these instruments necessitates a technique where the microscope and the vacuum-tube amplifier are essential parts of production and inspection equipment. Dynamic balancing of the small high-speed rotors required the development of a production balancer of novel design in which the rotor is operated, and the location of unbalance in either of two correction planes is marked at the location of metal to be removed by means of an electric arc. An indication of required depth to be drilled is secured and the rotor balanced by drilling out the prescribed amount at the marked spot by means of an integral drilling fixture on the machine. In this manner rotors of several ounces in weight are balanced to a residual unbalance not greater than 0.00001 ounce-inch.

The assembly of these instruments must be done in air-conditioned rooms with extreme emphasis on constant temperature, freedom from moisture, dust, and contamination. Investigation of sources of contamination indicate necessity for eliminating even the handling of parts with fingers and the development of technique, whereby vital parts are never touched with the hands. The use of cleaning methods and cleaning materials has been investigated fully to the extent that special equipment and specially prepared washing solutions have been attained and held to rigid specifications. The problem of testing, cleaning, and lubricating ball bearings was, in itself, a project of considerable scope requiring the development of highly specialized equipment and production techniques.

Gyro Horizon Indicator

This instrument may be used to fulfill two purposes. It serves to transmit signals in pitch and bank for automatic pilot control, or it may serve as a visual reference to the human pilot indicating the attitude of the plane. The unique feature of this instrument is its small size. Heretofore, the artificial horizon has been of larger size due to difficulties of manufacture and required accuracy. With the development of the small gyro vertical we have been able to produce an instrument of standard interchangeable size with other aircraft panel instruments (that is, $3^1/_8$ inches in diameter), which fulfills the requirements of accuracy for both indication and autopilot control. The gyroscope consists of an induction-type motor driven by three-phase 26-volt current supply. The motor draws five watts. The rotor of this instrument has a speed of 20,000 rpm. To secure the highest-possible moment of inertia, a centrifugally cast silver-alloy rotor with an integral squirrel cage is used. The gyromotor is mounted in a fork-type gimbal ring which, in turn, is mounted on a main pedestal in the rear of the instrument housing. Transmission of pitch and bank signals is accomplished by small signal generators on the pitch and bank axis. Conductors supplying power to the gyromotor and bringing out electrical signals are provided in the combination of contact pins and hairsprings. Some conception of the problems involved in the development of this instrument may be glimpsed from the fact that within the space of $3^1/_8$ inches in diameter by 6 inches in length is provided a gyro vertical of accuracy within 20 minutes of absolute vertical with freedom of ±100 degrees in bank, 80 degrees in dive, and 70 degrees in climb, transmitting signals of attitude in pitch and bank.

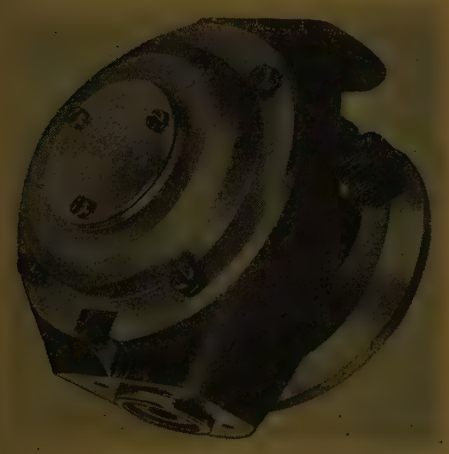


Figure 4. Principle of precessional gyroscope

Driftmeter

The gyro-stabilized driftmeter (see Figure 5) is an optical instrument designed to provide the navigator or bombardier with a stable reference with which to compute drift, ground speed, and bearings of terrestrial landmarks from the airplane independently of the pitch and roll of the aircraft.

The driftmeter consists of a vertical telescope through which the ground may be viewed and upon which is superimposed the image of a gyroscopically stabilized reticle. The system is so designed that, with pitch and roll of the aircraft, there is no relative motion between the image of the ground and the reticle, and so an observer can see what is apparently a stationary image against the reticle. The motion of the ground due to flight can be referred to reference lines on the reticle and accurate readings of drift obtained. The telescope is provided with scales in azimuth and in elevation, so that for certain purposes accurate bearings may be obtained. The gyroscopic element of the driftmeter is the large gyro vertical, consisting of the rotor of 5,000 grams centimeters squared moment of inertia, a rotor speed of 10,000 rpm, power supply of 400 cycles, 110 volts, single phase, and power consumption of approximately seven watts. This gyroscope does not contain any signaling devices, and current is brought into the gyromotor through the gimbal rings by means of insulated pins in hollow pivots concentric with gimbal bearings. The gyro is calibrated to an accuracy of better than ten minutes from true vertical.

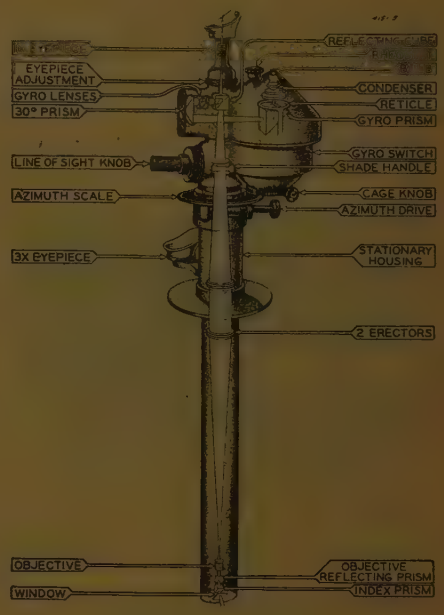


Figure 5. Driftmeter



Figure 6. Gyro roll indicator

Gyro Roll Indicator

The roll indicator (see Figure 6) is an example of the application of an aircraft

type of gyro vertical to marine use. For the purpose of fire control on board a medium-size vessel it was necessary to provide an accurate indication of the ship's roll with respect to true vertical. For this purpose the gyro used in the driftmeter was adopted. A special housing is provided and a pointer attached to the gimbal ring, the pointer being read against a scale on the stationary housing. The instrument is mounted with the outer gimbal bearing supports along the fore and aft axis of the ship and a reading of roll of the vessel is obtained within an accuracy of better than one-half degree



Figure 7. Electric turn-and-bank indicator

under conditions of rough weather and maneuvers.

Electric Turn-and-Bank Indicator

The turn and bank indicator (see Figure 7) is an instrument which combines the two functions that indicate the rate of turn of an aircraft and the resultant acceleration which is, in general, referred to as bank. The combination of these two indications results in correctly executed turns at proper banking angles. This instrument has been developed in three basic types:

1. Utilizing an air-driven gyroscope.
2. A d-c-driven gyroscope.
3. An a-c-driven gyroscope.

The principles and basic structure of the rate gyroscope have been described already under the heading "Precessional Gyroscope." In the d-c-driven instrument the gyroscope consists of a small d-c motor on the shaft of which is mounted a flywheel of sufficient moment of inertia to provide the required indicating characteristics against the centralizing springs. The motor current is supplied to the 24-volt shunt motor through contact pin connections to the case.

The a-c-rate gyroscope consists of an induction motor of which the rotor is the gyro mass. The rotor is exterior to the stator and rotates at a speed of 20,000 rpm. Power supply is 26 volts, three phase, 400 cycles at approximately four watts. The instrument serves as an indicator and also as a transmitter for aircraft control. At one end of the pivot axis is located a signal generator which provides electrical signals as a function of the angular velocity of the aircraft. Power supply and signal leads are in the form of hairsprings from gyroscope to case.

Gyro Flux Gate* Compass System

The Gyro Flux Gate compass system is a remote indicating earth-inductor gyro-stabilized compass. The system is composed of a gyro-stabilized Flux Gate transmitter, a master indicator, an amplifier, and repeater indicators. Its advantages are based on the fact that the magnetically sensitive compass element or Flux Gate is located in a position free from the magnetic and electrical disturbances usually found in the pilot's and navigator's stations, and that the Flux Gate is gyro stabilized and is thus maintained in a horizontal position at all times. As a result of this stabilization, turning and acceleration errors, for all practical purposes, are eliminated.

The Gyro Flux Gate transmitter (see Figure 8), with which we are concerned primarily, is a gyro vertical of the larger size, similar in electrical characteristics to the driftmeter type. Operating power is supplied at 110 volts, 400 cycles, 6 watts to the gyro induction motor. The motor stator is, again, inside the rotor and consists of a two-phase four-pole winding, one of the phases being in series with capacitors mounted on the gyromotor to provide the necessary quadrature current. In the bottom of the gyromotor housing is mounted a magnetic detector sensitive to the direction of the magnetic field in its plane. As a result, steady and accurate compass readings are obtained independently of pitch, bank, and maneuvers of the aircraft.

Automatic Pilot

This system comprises a combination of units, the purpose of which is the control of an airplane in attitude, direction, and through selected maneuvers. In the application of electrically driven gyroscopes to the automatic pilot, three of the instruments previously described are used. These are the artificial horizon, the Gyro Flux Gate transmitter, and the turn and bank indicator.

The artificial horizon transmits electrical signals of the attitude of the airplane with respect to the vertical to suitable devices for the purpose of control. These devices consist of amplifiers and servomotors which actuate the control surfaces of the airplane and maintain attitude.

The gyro-stabilized Flux Gate transmitter is also a two-purpose instrument in that it serves to transmit the magnetic heading of the aircraft for the purpose of indication and to provide a directional reference through amplifier and servo-system to the rudder-control surface.

The turn-and-bank indicator, likewise a dual-purpose instrument, serves to indicate the rate of turn-and-bank angle of the aircraft and to transmit the rate signals to combine with the directional signal of the Gyro Flux Gate system in such a manner that precise directional control is obtained. The rate signal is used further to control the desired rate



Figure 8. Gyro Flux Gate compass transmitter

of turn and to execute precision turns of correct banking angle.

To summarize, the electric gyroscope in aircraft instrumentation is rapidly supplanting air-driven types, owing to the following factors:

1. Freedom from air-supply requirements at high altitudes.
2. Sealing and elimination of air supply, thus preventing contamination and insuring greater reliability.
3. Greater versatility, especially where electrical signaling devices are required.
4. Compactness, greater reliability under extreme temperatures due to speed regulation and torque characteristics.

Since the development of electrically driven gyroscopes for instrumentation is a relatively new art, many interesting and very important applications still are to be anticipated.

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The Copper-Oxide Rectifier in Electrochemical Work

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ASSOCIATE AIEE

MUCH has been written about the copper-oxide rectifier during the 23 years of its existence, yet the subject is far from exhausted. It still remains one of the most interesting developments of recent years. A brief résumé of its history may be of interest.

The scene opens in 1921 when L. O. Grondahl discovered that a large area of copper coated with cuprous oxide would act as a rectifier. Five years later the rectifier known as the Rectox was introduced commercially for use in trickle charging "A" batteries in home radio sets. This became a tremendous business overnight but disappeared with the same disconcerting speed two years later with the advent of a-c tubes. Application of the rectifier to industrial uses started in 1927 and after a period necessary for trial and acceptance by users began to develop rapidly, reaching its peak during the past three years in many diversified war activities.

Though this may be familiar material to many, it will be worthwhile to run through those characteristics which particularly apply to the use of the rectifier in electrochemical work. A copper-oxide rectifying element is an integral unit consisting of a disk or plate of high-purity copper, coated on one or both sides with red (cuprous) oxide. The combination is shown schematically in Figure 1. It is an asymmetric resistor, that is, it passes current better in one direction than in the other. Current flows readily from oxide to copper, but with difficulty from copper to oxide.

This is best illustrated by a typical volt-ampere curve, which is simply a curve showing how much current flows in either direction for any given voltage, Figure 2. To illustrate, if one applies three volts in the low resistance direction, a current of nine amperes will flow through a $1\frac{1}{2}$-inch-diameter disk. If the voltage is then reversed, the current flow will be perhaps one milliampere, a ratio of 9,000 to 1.

From the shape of the curve, one can see that resistance in either direction is not a constant. This is better shown by Figure 3, on which is shown the variation in resistance with voltage for both directions of polarity.

These characteristics do not remain fixed but vary with temperature and also with time and operation. The forward resistance will tend to increase over a long period of time, the amount depending upon the temperature, while the back resistance will decrease somewhat during operation. These effects are known as aging. The effect of temperature is to cause both forward and back resistance to decrease as temperature increases, and vice versa.

The construction of copper-oxide rectifier units is by now familiar to many users. There are two general types, one involving a high degree of pressure on the rectifying element, the other requiring practically no pressure.

The pressure-type construction is applied to disk rectifiers used for relatively small capacities. A variety of such units is shown in Figure 4. The rectifying disks are strung on insulated bolts, with thin lead washers placed against the oxide surface. Radiating fins and metal spacers may be added, and the whole structure then pulled together under a pressure of over 1,000 pounds per square inch. This gives a solid, rugged type of unit that has stood the test of all kinds of service for many years.

The other type of construction is employed on plate-type rectifiers. A cross-sectional view is shown in Figure 5. The rectifier plate, to begin with, has oxide on both sides, whereas the disk is coated only on one side. The plate oxide is then reduced to copper on its outer surface, and the copper is plated over with nickel. Contact then must be made to both nickel surfaces. This is done with large spring washers, insulated from the plate copper, and pulled down and riveted together with a metal bushing. This construction thus parallels the two sides of the plate and furnishes the negative connection. The copper or positive connection is obtained by removing the oxide around a second hole through the plate. Figure 6 shows three sizes of plates with their connectors.

Plate rectifiers are used with natural convection cooling, but more often with forced draft. The plates are assembled on insulated studs, about one-half inch apart. This is ideal for forced cooling, as the cooling air comes in direct contact with nearly all of the active surface.

From one point of view, the method of determining a rectifier rating is of no interest to a user since he buys the rating he wants and expects that the manufacturer will have determined the rating properly. However, a user who does know more about the unit than just what is stamped on the name plate, can certainly operate with greater intelligence and with less likelihood of getting into trouble.

The method used is very simple to understand but the application of it is a matter involving considerable time and work. That, however, is the designer's problem.

Basically, the rectifier is a thermally rated device, like a transformer or a motor. Due to its internal resistance,

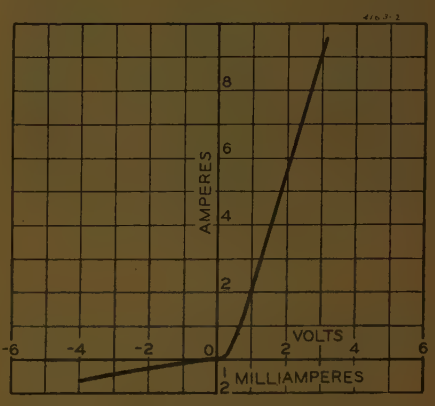


Figure 2. Typical volt-ampere curve for single copper-oxide disk

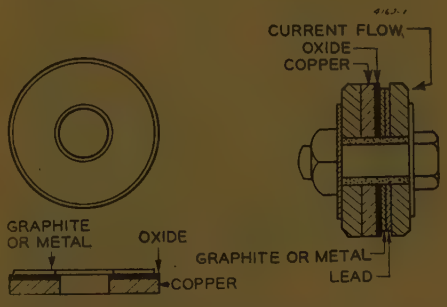


Figure 1. Diagrammatic cross section of single copper-oxide disk

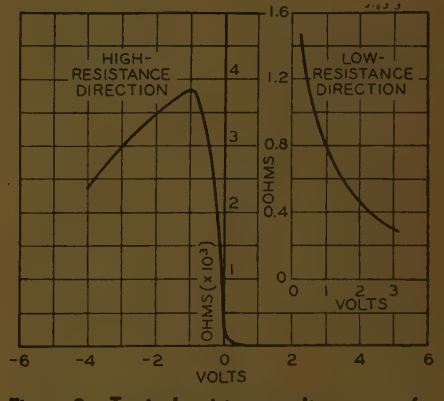


Figure 3. Typical resistance-voltage curve for single copper-oxide disk

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Figure 4. Group of Rectox disk-type units

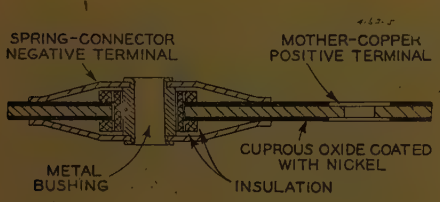


Figure 5. Diagrammatic cross section of Rectox plate



Figure 6. Standard copper-oxide plates with connectors assembled

heat is generated by the flow of load current in the forward direction and of leakage current in the back direction.

For any given rating, then, these losses are determined separately, either from volt-ampere curves or from actual tests, covering a range of operating temperatures. The separate losses are then combined into a total loss curve. In Figure 7 the general shapes of both the forward and back loss curves are shown, together with the total loss curve, which tells what the actual loss is at that rating and at any temperature in the operating range.

These curves, of course, must be based on aged characteristics, not new ones.

Having determined what the total loss curve is for the rating in mind, one still has to find out whether the type of construction intended to be used is satisfactory, that is, whether it has sufficient heat-dissipating ability to get rid of these losses. To learn this, the temperature rise of the particular structure is determined as a function of the watts to be dissipated in the structure. This gives a heat-dissipating curve which can be matched with the total loss curve to show what is the right combination.

How this works is shown in Figure 8. The heat-dissipating curve for the unit is curve *D*, drawn with the ambient temperature as a starting point. Also shown are three total loss curves. These are for the same total output. Curve *A*, however, is the total loss in a rectifier operating at high voltage and low current, curve *B* for one operating at high current and low voltage, and curve *C* for median values of voltage and current. Curve *C* corresponds to actual commercial ratings. Inspection of this figure shows that the combination of *A* and *D* is unsatisfactory. The two curves do not intersect and the rectifier would consequently burn up at this rating. Curve *B* intersects *D* at a broad angle and is obviously very safe since if temperature rises for any reason, losses will actually decrease. Curve *C* is also quite safe, crossing *D* at a reasonably wide angle.

Since curves *A*, *B*, and *C* are for the same output, their relative positions indicate the relative efficiencies. Curve *B*, it can be seen, has obtained exceptional safety at the expense of efficiency. Curve *C* gives good efficiency and satisfactory safety with a margin in case of occasional operation at higher than rated ambient temperature. Curve *C* also gives approximately the same losses and efficiency over the entire range of temperature, which is desirable. It follows also that the operating temperature, which is at the intersection of the loss curves and the heat dissipation curve *D*, is much lower for curve *C* than any other rating.

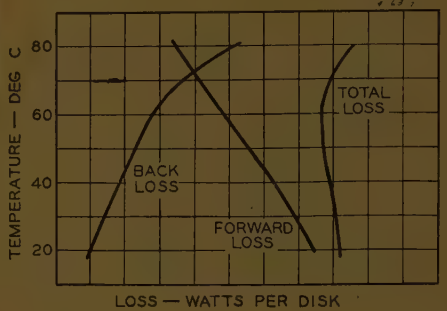


Figure 7. Relation of internal losses to unit temperature

With this brief discussion of characteristics, construction, and ratings, attention may be turned to applications, particularly in electrochemical fields.

Of course, the commonest of all electrochemical rectifier applications is battery charging. Probably nothing needs to be said beyond the fact that copper-oxide rectifiers are used successfully in all types of battery charging, ranging from a single cell at a fraction of an ampere to a 2,500-ampere 12-volt charger. Of interest is the 12-cell one-ampere oil-immersed charger shown in Figure 9. The use of oil immersion for protection against corrosive atmospheres is likely to expand rapidly. Figure 10 is included to show a fully automatic charger for industrial-truck batteries. This charger will handle up to 18 cells of lead-acid battery, and up to 450 ampere-hours capacity. The charger automatically gives the battery its proper rate, regardless of the state of charge. At the correct point, the rate is reduced to the desired finishing rate, and at the end of the finish charge, the rectifier shuts down and disconnects the battery. A companion model is available for nickel-alkaline batteries.

The next application is not so widely known except in the oil industry. That is the cathodic protection of pipe lines. It was found about 15 years ago by R. J. Kuhn and others that pipe-line leaks could be prevented if the pipe could be maintained cathodic, that is, negative to the surrounding soil. This was reasonable, since leaks occur due to loss of metal where the pipe is anodic, the soil being an electrolyte. To make the pipe cathodic, that is, to force it to be negative to the earth at every point, a source of direct current was connected between the pipe and ground plates in the surrounding earth. The system is shown simply in Figure 11. When such an installation is properly designed, the result is complete stoppage of leaks. The requirements are for relatively small-capacity units, delivering up to say ten volts at up to 100 amperes, the units being mounted on poles or platforms along the pipe line at intervals of the order of a mile. Operation, of course, is continuous.

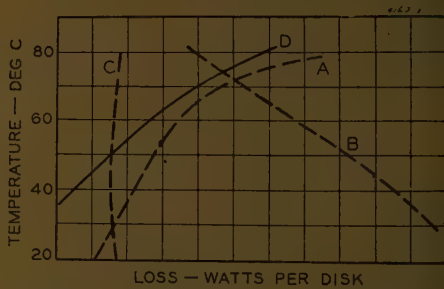


Figure 8. Heat-dissipation curve compared to total-loss curves for different disk ratings but same unit output

For this work the copper-oxide rectifier has been used since the beginning. It has good efficiency, which is important since operation is continuous and power rates are sometimes quite high. It can be mounted in a weatherproof box for outdoor service. It has no moving parts and requires no maintenance, and will operate for long periods without any need for adjustment. Figure 12 shows one of the many thousands of such units in service. Figure 13 illustrates a fan-cooled Rectox

built for this type of work for outdoor service.

A kindred application is the protection of water tanks and similar structures. Figure 14 shows a diagram of a typical installation. Electrodes placed in the water maintain a protective film of hydrogen over the inner surface of the tank, preventing corrosion and eliminating the need for painting. The units are of small capacity costing little to operate, and the installation as a whole is easy to justify economically. The requirements are similar to those for cathodic-protection rectifiers.

In the chemical and metallurgical field, electrostatic separators and Precipitrons are frequently used. These require a high-voltage source of small direct currents. Figure 15 shows a power supply for a metal separator which delivers 15,000 volts at a few milliamperes to the electrodes. The use of a copper-oxide rectifier is attractive in this field because of the elimination of tubes and tube replacements. The higher first cost of the copper-oxide unit is felt to be justified by the freedom from later maintenance and the cost of tubes.

In the field of anodizing and plating, rectifiers have reached great prominence during the war. This has resulted to quite an extent from the shortage in rotating equipment, so that many people had to use rectifiers for plating and anodizing who otherwise probably never would have tried them. Now that they have gained familiarity with rectifier operation and its advantages, it is believed rectifiers will be preferred for many plating applications.

In Figure 16 is shown a standard stock rectifier for plating work, rated six volts, 500 amperes maximum. It is fan cooled, takes less than 4 square feet of floor space, stands 38 inches high, and weighs about 600 pounds. It is designed so that one or two more units can be mounted on top, thus economizing on floor space for larger capacities. These units are connected in parallel or series, as desired, for larger outputs, and are controlled from a separately mounted step-type regulator. The regulator unit then is mounted close to the operator, while the rectifier cubicles are located where they are not subject to corrosive fumes. The units being fan cooled are exposed to large amounts of room air during their operation and cannot be used safely in a corrosive atmosphere.

The unfortunate effect of corrosive atmospheres on copper-oxide plates (in which in fact they are no different from other types of electric apparatus) led to the design of a totally enclosed rectifier which could be installed right in the plating room adjacent to the plating tanks. This type of rectifier was supplied for two continuous strip-tinning lines. The rectifiers are mounted two high and are right next to the plating tanks, which are also on two levels. This resulted in a



Figure 9. Oil-immersed Rectox battery charger
Rating one ampere, 12 cells



Figure 10. Automatic charger for industrial-truck batteries

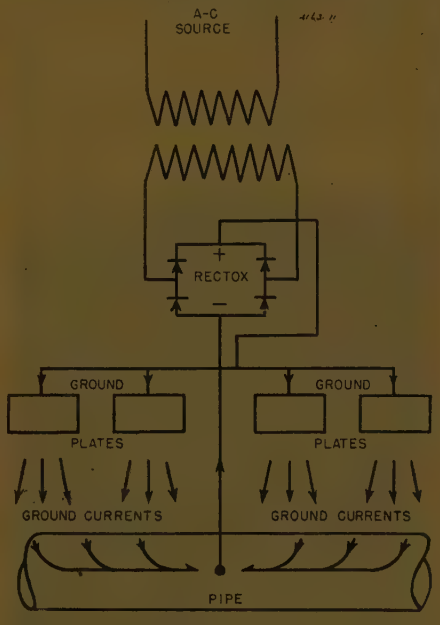


Figure 11. Diagrammatic representation of cathodic-protection system for pipe lines



Figure 12. Installation of pole-mounted Rectox for cathodic protection

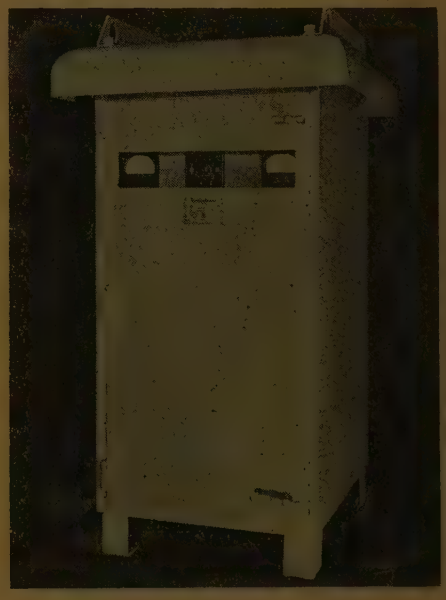


Figure 13. Outdoor fan-cooled plate-type Rectox for cathodic protection

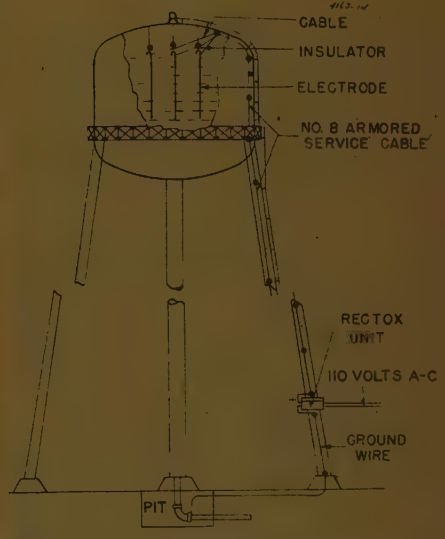


Figure 14. Diagrammatic representation of protective system for water tanks



Figure 15. A 15,000-volt five-milliampere Rectox for metal separator



Figure 16. Stock models of six-volt 500-ampere Rectox electroplating rectifier

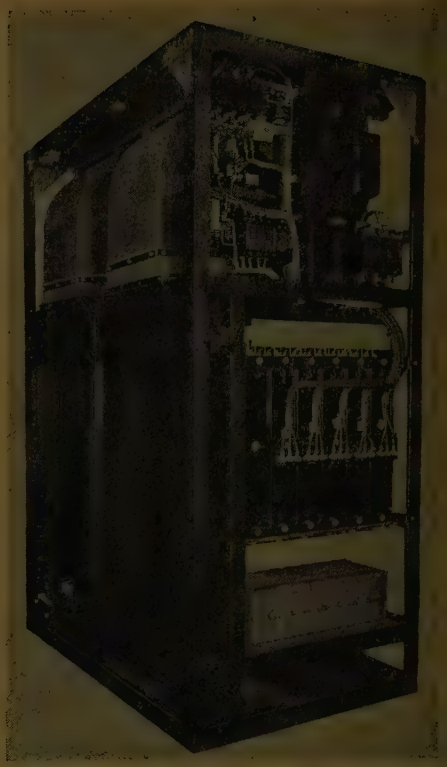


Figure 18. Totally enclosed rectifier showing arrangement of blowers, air duct, and control

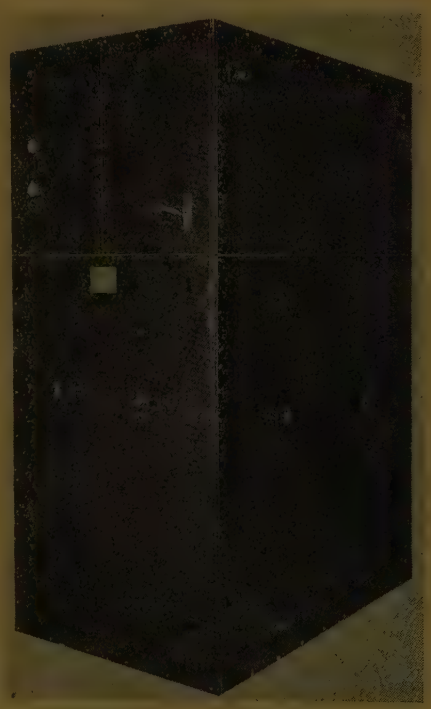


Figure 17. 12-volt 2500-ampere totally enclosed Rectox electroplating rectifier



Figure 19. Totally enclosed rectifier showing heat exchanger, Rectox plate assemblies, power transformer, and saturable reactors

tremendous saving in bus copper, estimated to have been 80,000 pounds.

Figures 17 to 19 show the construction of one of these rectifier units, rated to deliver six volts, 5,000 amperes, or 12 volts 2,500 amperes direct current. Ventilating air is circulated through the unit in a closed system by a double blower. Heat is removed by a water-cooled heat exchanger. The rectifier power transformer is located between the two six-volt 2,500-ampere blocks of rectifier plates. In the top of the cubicle are three saturable reactors, used to control the output of the rectifier over a range of ten to one in both voltage and current. Contactors controlling the power to the rectifier and to the blower motor, type *AB* disconnecting breakers for the rectifier and for the control circuit, and temperature and fan-failure relays are all located at one end of the cubicle for accessibility.

An automatic control system is used by which the output of all the 24 rectifiers in one line is varied in proportion to the speed of the strip through the mill. In addition, each rectifier has individual control so it can be varied independently of the others to take care of changing requirements resulting from wear of the particular anodes which this rectifier may feed. This tinning line has been in successful operation since the middle of 1943.

The performance of one of these units is shown in Figure 20. Over-all efficiency is shown for the condition of fixed load resistance in part B. This is the condition existing when the line is slowed down and the rectifier output is varied to match the line speed. Efficiency at fixed current is shown in part D, this being the case when the anode wears away, requiring a higher voltage, but the same current. Efficiency at constant voltage is also shown in part A, although this is not a condition which occurs in the operation of the plating line. Power factor for the fixed resistance load is shown in part B, the power factor being determined mainly by the saturable reactors in the primary circuit. Other curves show the variation in output in terms of the variation in the saturating current of the reactor, and the variation in reactor voltage and transformer secondary voltage for the range of output voltage. Saturable-reactor control has the advantage of lending itself well to automatic operation and also of giving the same smooth variation in output as is obtained by rheostatic control of a generator.

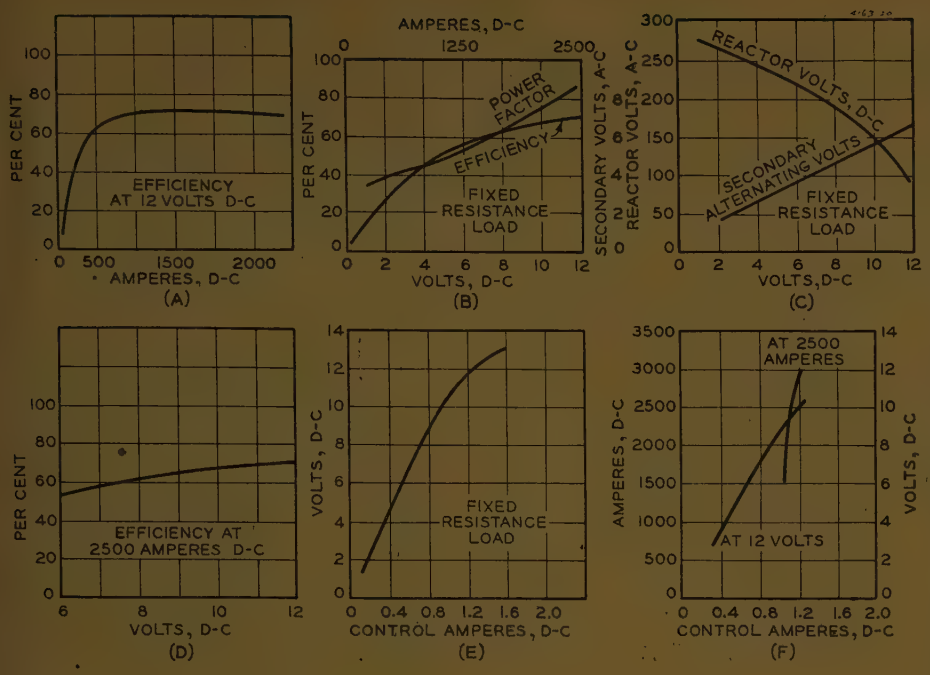


Figure 20. Performance curves of totally enclosed 12-volt 2,500-ampere rectifier

The circuits used in these applications are few in number and simple. Generally the ones involved will be found among those shown in Figure 21. It is not intended to discuss these here in any detail. Generally the single-phase and three-phase bridge circuits are used for electrochemical applications. Occasionally the full wave and the three-phase double wye may be used, their purpose being to obtain points of maximum efficiency at voltages half way between the maximum efficiency points of the bridge. Thus, if the bridge circuit gives maximum efficiency at 6, 12, 18 volts, and so on, the full-wave or the double-wye circuits will give maximum efficiency points at 3, 6, 9, 12, and so on.

In electrochemical applications, involving often continuous operation or operation over long periods, efficiency of the rectifier is of prime importance. This is true not only for full rating but for voltages below full rating. It will be realized that many six-volt plating rectifiers operate at only three or four volts. In cathodic-protection work a survey made several years ago of some 3,000 units in service showed that 75 per cent were operating at less than one-half their voltage rating, while only 2 per cent were at or near full rating. It is peculiar to this application that full output may be required when operation begins, but that after a period the power-output requirements reduce greatly due to polarization of the pipe line.

Hence the efficiency is important at partial voltages as well as at full voltage. In this respect, the copper-oxide rectifier is favored because of its low forward resistance, resulting in less reduction in

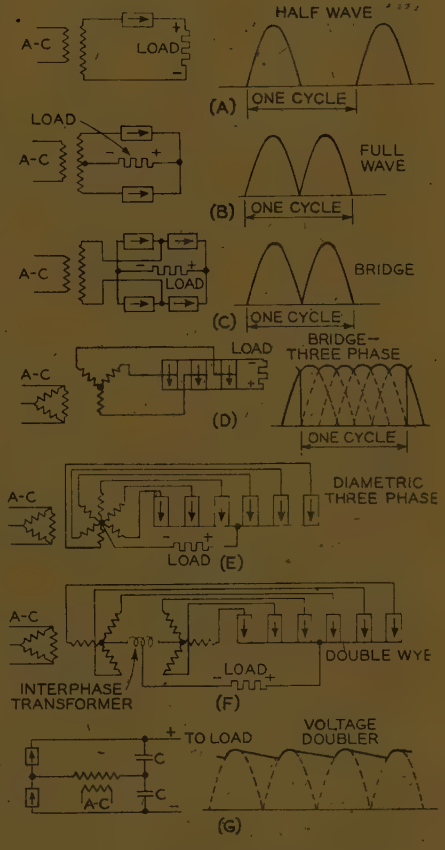


Figure 21. Circuits commonly used for metallic rectifiers

efficiency for partial voltages than is the case with some other types of rectifiers. Figure 22 shows how efficiency varies with voltage output, load being constant.

Long life is also a very desirable feature in electrochemical work. In this respect, the copper-oxide rectifier is supreme in its field, measuring its life in years rather than in hours.

Figure 23 is given to show how little actual effect aging has on performance. In November 1939 the first large electroplating Rectox was installed in which the plates had the plated-nickel contact coating. In Figure 23 the performance of this rectifier when installed is shown as curve *A*, while the performance after $4^1/_2$ years of service is shown on curve *B*. Study of these two curves shows there has been no discernible effect due to forward aging. The slight reduction at light loads is due to what aging has occurred in the back direction. Full load is obtained on exactly the same transformer tap now as when installed. Voltage regulation consequently shows no apparent change over the period. Prevailing ambient temperature for this rectifier is about 25 degrees centigrade.

This installation illustrates well the advantages of a plating rectifier. Here is a power plant that has operated over a $4^1/_2$-year period with the same full-load performance at all times, and with no expense to the user as yet for maintenance or repair of the rectifying units.

In Figure 24 is a typical life curve of the disk-type Rectox units used in operating d-c elevator brakes and control. This curve was taken on a large number of units in the field and shows a practical

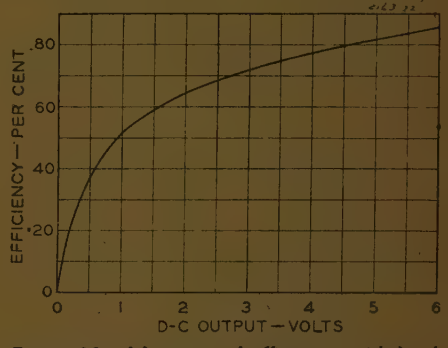


Figure 22. Variation of efficiency with load voltage at constant load for three-phase bridge Rectox

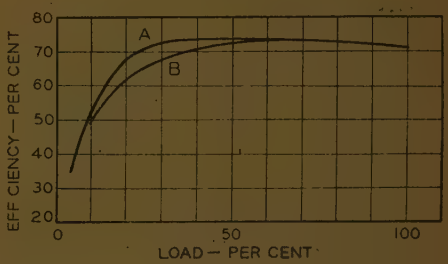


Figure 23. Efficiency curves showing effect of $4^1/_2$ years of service on a six-volt 2,000-ampere plating rectifier

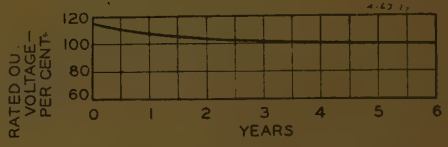


Figure 24. Field test on Rectox units supplying power for elevator control and brakes

Fundamentals of Hearing-Aid Design

W. D. PENN
ASSOCIATE AIEE

A HEARING AID is a miniature sound-amplifying system which is worn on the person in order to help overcome the handicap of impaired hearing. To be effective a hearing aid must be capable of amplifying those frequencies necessary for intelligibility of speech and reproducing them at a high-intensity level so as to make normal conversation audible to the person with impaired hearing. Modern hearing-aid amplifiers are of the vacuum-tube type, using small tubes designed specifically for them. A complete hearing aid consists of a microphone, a vacuum-tube amplifier, a receiver, and the necessary battery supply and connecting cords. Usually the microphone and amplifier unit are housed in a single case and the receiver and battery supply are connected to the amplifier by means of flexible cords. In order to determine the amount of amplification necessary, the type of frequency response needed, and the sound-pressure output required, it will be necessary to consider in some detail the different types of partial deafness and the characteristics of speech and hearing.

The Nature and Extent of Partial Deafness

Surveys of the population of the United States have shown that a considerable portion of the people have some form of defective hearing.[1] From the data collected it is possible to estimate the percentage of the population having a hearing loss greater than any particular designated value. Approximately four per cent have losses sufficient to require the use of a hearing aid. It is found that so-called normal hearing depends upon the age of the individual. In general with advancing age there is a decrease in acuity for high-frequency tones.

Partial deafness may be classified as transmission deafness, nerve deafness, or central deafness.

Transmission deafness is caused by failure of the mechanism of the outer and middle ear properly to transmit acoustic vibrations to the hearing nerve centers. This type of deafness may be due to a defective ear drum, an obstruction in the ear canal, failure of the small bones of the ear to function normally, or any one of a number of other reasons.

Nerve deafness is caused by failure of the nerves of the inner ear to respond to acoustic stimuli after the latter have been transmitted to the inner ear.

Central deafness is due to the failure of the brain to interpret and co-ordinate sound patterns that have been received properly by the hearing mechanism.

A particular individual may suffer from a combination of any of the three types of partial deafness.

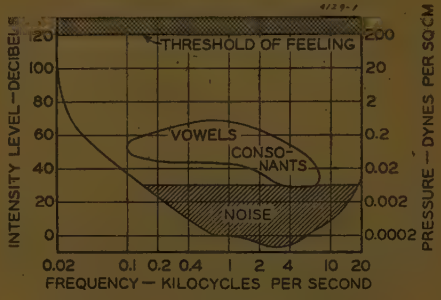


Figure 1. The auditory area

The Measurement of Hearing Loss

Many methods have been devised for measuring the acuity of hearing. Present-day practice is to use an audio-frequency oscillator in conjunction with a telephone receiver which has been calibrated in terms of its acoustical sound output. Such a device is called an audiometer.[2] Audiometers are designed to provide definite sound-pressure outputs over a range of frequencies from 128 cycles per second to 8,192 cycles per second. A calibrated attenuator is provided whereby the sound pressure can be adjusted in five decibel steps above the reference level. The reference level is taken as the minimum audible sound pressure for normal hearing for persons of 18 to 23 years of age.

It is possible to transmit sound vibrations directly to the nerves of the inner ear by means of a mechanical vibrator placed in contact with the skull. Such a device is called a bone-conduction receiver (or oscillator). This method of transmission circumvents the outer and middle ear. By using such a unit for output receiver of a properly calibrated audiometer, it is possible to measure that portion of the total hearing loss due only to defective hearing nerves. The audiometer thus provides a means of measuring the minimum sound pressure required to evoke a sensation of hearing either by air conduction (telephone receiver) or by bone conduction. A curve showing the minimum audible sound pressure at different frequencies is called an audiogram. The audiometer measures the minimum sound pressure required for audibility and really is a measure of what is *not* heard rather than what is heard.

In estimating an individual's capacity for understanding speech from his audiogram, it is necessary to take into consideration the relative effects of different frequencies on the intelligibility of speech.[3]

The Auditory Area

The frequency range for normal hearing has been established as being from 20 to 20,000 cycles per second. The ear does not respond uniformly to sounds of different frequencies, so the minimum sound pressure required for audibility varies over the frequency range. The curve showing the minimum audible sound pressure is called the threshold of audibility curve. If at any frequency within the hearing range the sound pressure is increased progressively above the minimum audible pressure, an upper limit at which a sensation of pain occurs is reached. This upper limit is relatively

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stabilization of the output voltage with time. It is interesting to see that for the three-to-six-year period there seems to have been no change in output. These same units by now have an additional seven years of life. Records, however, were not kept after the six-year point was reached.

Laboratory units are still running which were started in 1926–28, so it is correct to say that there appears to be no limit to the life of a copper-oxide rectifier.

Conclusion

Electrochemical applications characteristically require high operating efficiency at all voltages up to full rating, operation free from maintenance, and long life. Some of these applications are among the oldest in which the copper-oxide rectifier has been used, and in which it has achieved great success. Familiarity with the rectifier is daily increasing, due to the much enlarged use of the past few years. It is believed that this ground will not be lost, but that the advantages of the copper-oxide rectifier will result in further expanding its use as these advantages become more generally recognized.

constant with frequency. The curve showing the maximum sound pressures is called the threshold of feeling.[4]

The area enclosed by the threshold of audibility and threshold of feeling curves, between the upper and lower limits of frequency is known as the auditory area. For a sound to be heard by an individual it must fall within the auditory area of that person. Usually there will be some particular intensity level in the auditory area of the partially deafened person which provides the most comfortable hearing. If a hearing aid is used, the intensity can be adjusted to the proper level by means of the volume control.

Characteristics of Speech

The prime purpose of a hearing aid is to amplify speech sounds so that maximum intelligibility results for the user of the aid.

The sounds of speech are composed of combinations of different tones spread throughout a range of frequencies from 100 cycles per second to 8,000 cycles per second. The vowel sounds embrace a range from approximately 100 to 2,500 cycles per second. Consonants consist mainly of high-frequency tones extending from 1,000 to 8,000 cycles per second. Each sound of speech is characterized by certain groups of frequencies and, unless these groups are present in the proper proportions, the speech will not be intelligible no matter how loud it may be.

Some frequency bands are more essential to the understanding of speech than others. The low frequencies of speech furnish the greater part of the acoustic energy. The high frequencies contribute the greater part of the intelligibility. Thus eliminating all frequencies below 500 cycles per second reduces the distinctness by only four per cent while reducing the acoustic energy by 62 per cent. On the other hand, eliminating all frequencies above 500 cycles per second reduces the distinctness or intelligibility by 93 per cent and removes only 38 per cent of the energy. There is but little loss of intelligibility if the range from 200 to 4,000 cycles per second is transmitted.[5]

Studies of the intensities of the various frequencies present in normal conversational speech show that they are distributed throughout the auditory area approximately as indicated in Figure 1.[6] The left hand ordinate of Figure 1 gives the intensity in decibels above a reference intensity of 10^{-16} watts per square centimeter. The reference pressure is thus 0.0002 dyne per square centimeter or 0.0002 bars. The right hand ordinate gives the pressure in bars. Examination of Figure 1 indicates that the principal voice frequencies vary in sound pressure from a minimum of 0.006 bar to a maximum of 0.6 bar. Expressed in decibels this would correspond to a volume range of 40 decibels. There will be some variation of volume range between individual speakers. For recognition of speech a volume range of 30 to 40 decibels is essential. If a hearing impairment causes the threshold of audibility to be so near the threshold of feeling that the auditory area does not encompass a range of 40 decibels, it will be impossible to understand speech even though it be amplified sufficiently to place it above the threshold of hearing.

Need for Selective Amplification

It has been pointed out that partial loss of hearing occurs in various forms. No two people with the same type of partial deafness will suffer exactly the same hearing loss at each frequency. In general, those suffering from nerve deafness hear low frequencies better. They hear sounds but do not understand the words because the high-frequency sounds are essential for intelligibility.

Unless the hearing loss is uniform throughout the frequency range it is necessary to utilize frequency selective amplification. Even though the hearing loss is uniform, it is advantageous in many instances to use the proper type of frequency selective amplification. If selective amplification is not used, then the frequencies which the individual has no difficulty in hearing will be amplified equally as well as those with which he has difficulty. The relative proportions of the various frequencies comprising the sound then will not be correct and some of the frequencies will become so loud as to be annoying. They might reach even the upper threshold of feeling before the other frequencies are sufficiently loud to indicate the characteristics of the speech sounds. Examination of Figure 1 shows why this occurs. If the areas representing the speech sounds are moved uniformly upward, the vowel sounds strike the feeling curve long before the consonants. Thus the vowel sounds will tend to mask the weaker consonant sounds which are essential for intelligibility if uniform amplification is used.

It appears then that for good intelligibility some form of frequency selective amplification must be used. Previous considerations of the characteristics of speech and hearing have shown that for intelligibility the high speech frequencies are necessary and that the low frequencies are not so important. From the manner in which the speech sounds are distributed in the auditory area, it is also apparent that when the hearing loss is severe it is necessary to attenuate the low frequencies in order to be able to bring the high-frequency sounds above threshold and still not have the low frequencies strike the threshold of feeling.

Attenuation of the low frequencies usually results in some increase in intelligibility even when the hearing loss is uniform. This is because in average room

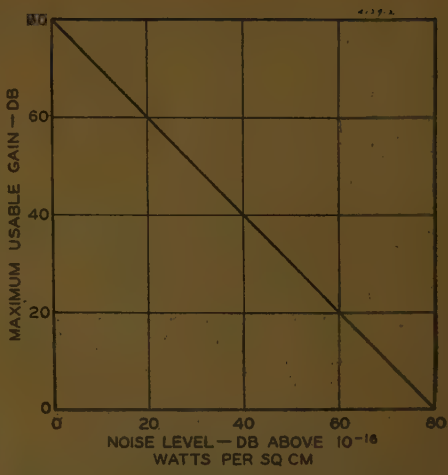


Figure 2. Maximum usable gain as a function of noise level

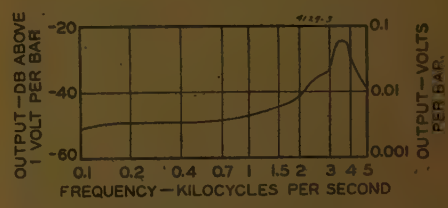


Figure 3. Response characteristic of hearing-aid microphone

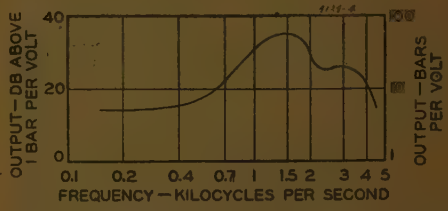


Figure 4. Response characteristic of hearing-aid receiver

noise the intensities are higher for the low frequencies which thus produce a masking effect which can be lessened by reducing the intensity of these components.

From the above considerations it is reasonable to conclude that the type of selective amplification used should be such as to make possible attenuation of the low-frequency end of the spectrum. The control should be of the variable type since each case will be somewhat different.

Output Requirements

The maximum sound-pressure output which a hearing aid must be capable of producing can be determined from the chart of the auditory area. If uniform amplification is used, the speech level can be raised until the frequencies in the region from 500 to 1,500 cycles per second strike the upper threshold of 200 bars. The higher frequencies will then have pressures of from two to 20 bars. If the frequencies below 1,500 cycles per second are attenuated, it is possible to raise the output pressure at high frequencies with-

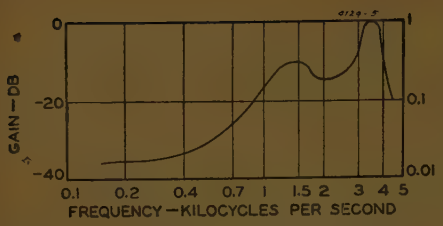


Figure 5. Combined response characteristic of microphone and receiver

out having the low frequencies strike the threshold of feeling.

The auditory area as shown in Figure 1 is the average of many normal ears. A particular individual will exhibit certain variations from the average. In transmission deafness it is probable that the threshold of feeling also is raised. The above figures nevertheless serve as an approximate guide in determining the output requirements.

The maximum hearing loss which can be overcome at any frequency is determined by the sound-pressure output that can be obtained at that frequency. Thus, if an individual has a uniform hearing loss of 60 decibels the speech sounds must be raised in intensity so that all of them are above a 60-decibel intensity level. Since the range between the loudest sound and the weakest is 40 decibels the loudest sounds must have a level of 60+40 = 100 decibels. For the high-frequency sounds above, for example, 2,000 cycles per second an output level of 60 to 80 decibels would be required.

As a rough guide, it is seen that for the case of a uniform loss and uniform amplification the required output level in decibels, L, must equal the hearing loss, HL, in decibels plus 40. That is

$$L = HL + 40 \qquad (1)$$

From equation 1 it would appear that with uniform amplification and uniform loss, the greatest hearing loss which could be overcome would be 80 decibels and in order to overcome a loss of this magnitude the hearing aid would have to be capable of supplying a pressure of 200 bars for frequencies less than 1,500 cycles per second and 20 bars for frequencies greater than 1,500 cycles per second.

If, as is often the case, the hearing loss increases with frequency then in order to bring the high-frequency components necessary for intelligibility above the impaired threshold and still not let the low frequencies reach the threshold of feeling, it is necessary to attenuate the low frequencies.

If the loss is greater for low frequencies the requirements are not so severe because this does not reduce greatly the intelligibility. Also, as mentioned previously, the type of hearing loss characterized by decreased acuity for low tones is usually due to transmission deafness and the upper threshold is raised so that uniform amplification can be employed with less possibility of striking the upper threshold of feeling. A recent tabulation of audiograms indicates that only 13 per cent of those tested had characteristics which showed a loss greater for low frequencies than for high frequencies. Increasing loss with frequency was shown by 43 per cent and 44 per cent had a uniform characteristic. Ten per cent of the total number showed a loss greater than 80 decibels.[7]

Nonlinear Distortion

If the relation between output sound pressure and input sound pressure is not linear, nonlinear or amplitude distortion occurs. Vacuum-tube amplifiers can be made to operate as substantially linear devices if certain limits of operation are not exceeded. However, no amplifier has an unlimited output capacity and so as the input signal is increased progressively a point finally is reached beyond which the output is not directly proportional to the input. Since the amplitudes are largest in the output stage, most of the distortion occurs there. As a result of the nonlinear relation between input and output, new frequencies are created in the output which are not present in the original signal.[8] Under such conditions the output sound pressure is not a faithful replica of the input signal. The extraneous frequencies present mask the original sounds thus reducing the distinctness of the speech. The maximum usable output obtainable from a given amplifier is limited by the amount of nonlinear distortion which can be tolerated.

Measurements of the amplitude distortion necessary to produce a detectable change in speech quality show that the amount of distortion which can be tolerated depends upon the band width of the amplifying system, the type of distortion (even or odd harmonics), and whether or not the distorted speech is compared directly with the undistorted speech.[9] With an upper cutoff frequency of 5,000 cycles per second it is found that from 12 to 17 per cent distortion can be introduced before a noticeable change in speech quality occurs. With an upper cutoff of 8,000 cycles per second the permissible distortion is reduced by approximately one half. For complex signals such as speech, the per cent distortion is defined as the amount of distortion produced by a pure tone having a peak value equal to the peak value of the complex wave. Satisfactory reproduction of speech will be obtained if the distortion is held below 15 per cent.

Gain Requirements

It is necessary to distinguish between output and gain. To be of practical use an instrument must develop the required output pressure at the ear when the source of sound is some distance away.

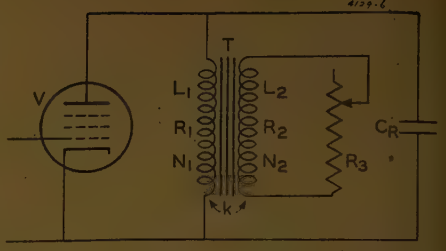


Figure 6. Circuit for controlling frequency response

The greater the amplification the less input signal the hearing aid will require to develop a given output pressure. At first thought it might appear that the maximum obtainable gain would be limited only by the number of stages of amplification which could be used in an instrument of wearable size. In practice there are two other factors which determine the maximum *usable* gain. The first of these is the surrounding noise level. Numerous measurements have shown that the extraneous noise level in the average quiet residence has an intensity level of about thirty decibels. In commercial buildings the noise level will be higher.[10] The effect of this extraneous noise level is to raise the threshold of hearing. A normal ear immersed in a noise level of 30 decibels would be unable to understand conversational speech unless the speech intensity level was greater than 30 decibels. In normal speech (at conversational distances) the loudest vowel sounds are 70 decibels above threshold and the weakest consonant sounds 30 decibels above threshold and thus they would be heard in spite of a noise level of 30 decibels. A hearing loss of 30 decibels would have the effect of eradicating the surrounding noise level but ordinary conversation still would be audible. If the source of sound were farther away, the desired sound would have a level less than the surrounding noise and no amount of amplification would help, since the noise and desired sound both would be amplified. The only solution is to have the desired signal louder than the background noise. Thus the auditor always must move close enough to the source of the desired sound to make its intensity level greater than that of the surrounding noise level. At conversational distances the speech sounds are located as shown in Figure 1. It is seen that the pressure of the loudest vowel sounds at the hearing-aid microphone will be 0.6 bar. To overcome the most severe case of deafness this pressure must be raised to 200 bars. This requires a pressure ratio or acoustical amplification of 200/0.6=333.3 which corresponds to approximately 50 decibels. The other weaker sounds, assuming uniform amplification, will be raised by 50 decibels also.

If the surrounding noise level is lowered, the distance between speaker and

listener can be increased and the speech still will be above the noise level. The speech intensities arriving at the microphone will be less, and correspondingly greater gain will be required for the same sound-pressure output. On the other hand, if the noise level is increased, the distance between speaker and listener must be decreased in order to raise the speech sounds above the surrounding noise. The speech pressure incident upon the microphone now will be greater and less gain will be required for a given sound-pressure output. Thus the maximum usable gain is determined by the surrounding noise level. Under average conditions of 30 decibels noise level the speech intensities would be as shown in Figure 1 and the maximum usable gain would be 50 decibels. In order to hear in any given noise level either the speaker must raise his voice to an intensity above the noise level or the distance between the speaker and listener must be decreased. In any event, for a given noise level the speech must have a definite sound pressure if it is to be heard. Since the distance between threshold and feeling is approximately 120 decibels, the maximum usable gain in decibels is

$$\text{Maximum usable gain} = 120 - (\text{noise level} + 40) = 80 - \text{noise level} \quad (2)$$

All quantities in the above expression are in decibels. Figure 2 is a graph of equation 2.

Acoustic Feedback

It previously was stated that, in practice, two factors limit the maximum usable gain. One of these has been shown to be the surrounding noise level. The second limiting factor is acoustic feedback. A hearing aid produces a sound-pressure output many times greater than the input pressure. If a portion of the output is fed back into the input in proper phase and magnitude, oscillation results. When acoustic feedback or "squealing" occurs, the instrument ceases to be of use to the user. Hearing-aid receivers are of the insert type. They are held in place at the ear by means of a plastic mold, the tip of which extends into the ear canal. With an ideal ear mold all of the sound pressure would be delivered to the ear drum. In practice, due to the imperfect fit of the ear mold a certain amount of acoustic energy may escape and find its way back to the microphone. If at any frequency the attenuation in the return path from receiver to microphone is less than the acoustical gain of the instrument, oscillation occurs.

When individually molded ear tips are used a gain of 60 to 70 decibels is ordinarily the maximum which can be had without feedback.

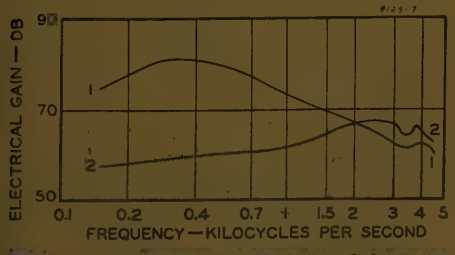


Figure 7. Frequency response of hearing-aid amplifier

Computation of Acoustical Gain

The over-all acoustical amplification of a hearing aid will be affected by the characteristics of each of the component parts of the instrument.

It is customary to specify the output level of a microphone in terms of its open-circuit output voltage in decibels above a reference level of one volt per bar. Figure 3 shows the output of an uncased hearing-aid microphone of the crystal type as a function of frequency. When the microphone is enclosed in a case the response is altered somewhat by the acoustical properties of the case and this effect often can be used to advantage. The acoustical properties of the case are not included in the following analysis. Receiver output level can be expressed in decibels above a zero level of one bar per volt. The response characteristic of a crystal-type hearing-aid receiver is shown in Figure 4.

The response characteristics of the microphone and receiver may be combined into a single curve which then will show the ratio of output pressure to input pressure of the two units acting together. This ratio will be less than unity and, when expressed in decibels, is a negative number. The combined characteristic of the microphone of Figure 3 and receiver of Figure 4 is shown in Figure 5.

After the microphone and receiver have been selected, the electrical gain of the amplifier necessary for a specified acoustical gain can be determined.

In order to develop an expression for the over-all acoustical gain of microphone, amplifier, and receiver the following definitions are necessary.

Let

K_M = open-circuit output of microphone in volts per bar

K_R = output of receiver in bars per volt

$K_{MR} = K_M K_R$ = ratio of output pressure to input pressure of microphone and receiver with no amplification between

P_o = output pressure of receiver in bars

P_i = input pressure to microphone in bars

VA = voltage amplification of amplifier unit

$G = P_o/P_i$ = over-all acoustical gain

N_A = over-all acoustical gain in decibels

Then

$$P_o = (P_i)(K_M)(K_R)(VA) \quad (3)$$

and

$$G = P_o/P_i = (K_M)(K_R)(VA) = (K_{MR})(VA) \quad (4)$$

In decibels the gain is

$$N_A = 20 \log_{10} G = 20 \log_{10} (K_{MR}) + 20 \log_{10} (VA) \quad (5)$$

Letting

$N_{VA} = 20 \log_{10} VA$ and $N_K = 20 \log_{10} K_{MR}$ equation 5 becomes

$$N_A = N_{VA} + N_K \quad (6)$$

Knowing the desired over-all gain N_A and the combined characteristic N_K the necessary voltage amplification of the amplifier expressed in decibels is obtained from equation 6 as

$$N_A = N_{VA} - N_K \quad (7)$$

N_K is a negative quantity so the required electrical gain is greater than the desired acoustical gain by N_K decibels. The necessary electrical gain thus is established in terms of the desired acoustical gain and the characteristic of the microphone–receiver combination. For the microphone–receiver combination of Figure 5, $N_K = -33$ at 500 cycles per second. To achieve an acoustical gain of 50 decibels at 500 cycles per second would thus require a decibel voltage gain, N_{VA}, of 83.

Control of Frequency Response

It has been shown that in many instances selective amplification is desirable in a hearing aid. There are numerous methods for controlling the frequency response of an amplifier. Most of these are not applicable to hearing-aid use because of the additional circuit components necessary. In the hearing-aid industry it is common practice to secure frequency selectivity by using a number of interchangeable parts each having different response characteristics, or to manufacture a number of different models each with a different response. These methods have the disadvantage that a multiplicity of extra parts must be manufactured and stocked in order to meet the requirements of people with different types of deafness. A further disadvantage is that at the time of purchase it is often difficult to determine which combination eventually will give the most satisfactory results. It might seem that the best of a number of available instruments could be selected by a listening test. Experience has shown, however, that methods of fitting a hearing aid which comprise selecting one of a group of available characteristics

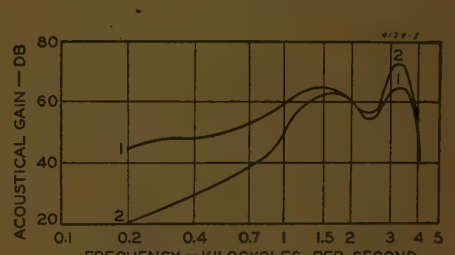


Figure 8. Acoustical gain of hearing aid

by listening test are not adequate. This is because the individual is accustomed to hearing sounds as they are rendered through his defective hearing mechanism. Thus, an instrument which makes sounds seem similar to those he is accustomed to hearing usually is chosen. As a result his impairment actually is exaggerated. It often is found that, after the user has become accustomed to an instrument, readjustment of the frequency response results in greater intelligibility. It is difficult to make such adjustments if they involve the exchange of an instrument or part of an instrument.

A novel method of controlling the frequency response of a hearing aid whereby the low frequencies can be attenuated while the high frequencies actually are accentuated now will be described.[11] This method is particularly suitable when used in conjunction with receivers which have a capacitative internal impedance, such as a crystal receiver. The method has the advantage that no interchangeable parts are necessary, and the variation in response is continuous throughout the range of operation.

The output stage of a hearing-aid amplifier usually is coupled to the receiver by means of a transformer or by means of a choke in order to obtain the largest possible output with a given "B" battery supply voltage. If a crystal receiver is used the internal capacity of the receiver and the inductance of the output choke form a parallel circuit with a resonant frequency determined by the LC product of the choke and receiver. The impedance of this parallel circuit is highest at resonance and maximum gain thus occurs at the resonant frequency. The frequency at which maximum gain occurs can be changed by varying the resonant frequency. The internal capacity of the receiver is fixed so the frequency of resonance must be changed by a variation of inductance. This could be done by tapping the coil but the number of resonant frequencies obtainable in this manner is limited.

Consider the circuit of Figure 6, in which the plate load of the output-tube V consists of the primary of transformer T in parallel with a receiver of internal capacity C_R. The secondary winding of T is connected to a variable resistance R_3. Considering the transformer by itself, the complex impedance looking into the primary is

$$Z_{12}=R_1+\frac{(M\omega)^2R_S}{R_S^2+(L_2\omega)^2}+ J\left\{L_1\omega-\frac{(M\omega)^2L_2\omega}{R_S^2+(L_2\omega)^2}\right\} \quad (8)$$

$$=R_{12}+JL_{12}\omega \quad (9)$$

In equation 8,

$$R_S=R_2+R_3 \quad (10)$$

The apparent resistance of the primary is thus R_{12} and apparent inductance is L_{12}.

Let

$$K=\frac{M}{\sqrt{L_1L_2}} \quad (11)$$

and

$$N=N_1/N_2\approx\sqrt{L_1/L_2} \quad (12)$$

Substitute equations 11 and 12 in 8 and obtain

$$Z_{12}=R_1+\frac{K^2N^2R_S}{1+(R_S/L_2\omega)^2}+ JL_1\omega\left\{1-\frac{K^2}{1+(R_S/L_2\omega)^2}\right\} \quad (13)$$

In terms of the coefficient of coupling, K, and the turn ratio, N, the apparent resistance looking into the primary is

$$R_{12}=R_1+\frac{K^2N^2R_S}{1+(R_S/L_2\omega)^2} \quad (14)$$

The apparent inductance is

$$L_{12}=L_1\left\{1-\frac{K^2}{1+(R_S/L_2\omega)^2}\right\} \quad (15)$$

Equation 15 is the basis of the selective circuit under discussion. If the secondary winding of T is short-circuited $R_S=R_2$. By suitable transformer design $R_2/L_2\omega$ can be made small compared to unity. Under these conditions equation 15 reduces to

$$L_{12}=L_1(1-K^2)=L_S \quad (16)$$

The quantity L_S is the leakage inductance of the transformer referred to the primary winding. The coefficient of coupling, K, depends upon the physical arrangement of the two coils L_1 and L_2 and upon the magnetic circuit on which they are wound. For coils wound on ferromagnetic cores, K can be made close to unity. L_S is then a few per cent of L_1.

With the secondary of T open-circuited ($R_3=\infty$) equation 15 becomes

$$L_{12}=L_1=\text{self-inductance of primary winding} \quad (17)$$

From equations 16 and 17 it is seen that any inductance value between L_1, the self-inductance of the primary winding, and L_S, the leakage inductance, can be obtained merely by varying the resistor R_3.

In the circuit of Figure 6 the primary of T is shunted by C_R, so the amplifier stage may be tuned to different resonant frequencies by means of a single variable resistor. Because of the resistance term, R_{12}, the resonance curves are very broad. The highest resonant frequency obtainable is

$$f_h=\frac{1}{2\pi\sqrt{L_SC_R}}=\frac{1}{2\pi\sqrt{L_1(1-K^2)C_R}} \quad (18)$$

The lowest resonant frequency obtainable is

$$f_l=\frac{1}{2\pi\sqrt{L_1C_R}} \quad (19)$$

The ratio of these two frequencies is

$$f_h/f_l=\frac{2\pi\sqrt{L_1C_R}}{2\pi\sqrt{L_1(1-K^2)C_R}}=\frac{1}{\sqrt{1-K^2}} \quad (20)$$

Thus any desired ratio of f_h/f_l can be obtained by providing the proper coefficient of coupling. The frequency response of the amplifier then can be controlled by varying R_3.

Performance of a Particular Hearing Aid

The curves of Figure 7 show the electrical response of a three-tube hearing-aid amplifier. The last stage of the amplifier uses the method previously described for varying the frequency response. Curve 1 is for R_3 open-circuited and curve 2 is for R_3 short-circuited. Any response characteristic lying between the two curves can be obtained by varying control R_3. Figure 8 shows the over-all acoustical amplification of a three-tube hearing aid having the electric gain shown in Figure 7. This instrument uses the microphone and receiver combination of Figure 5. Curve 1 of Figure 8 is obtained with the frequency control resistor, R_3, open-circuited. Curve 2 is obtained with the control resistor in the short-circuited position. The two curves indicate the maximum variation in response which is obtainable with this particular instrument. These curves were obtained using a 30-volt "B"-battery supply. At a frequency of 1,000 cycles per second an output pressure of 350 bars can be obtained with less than five per cent distortion when using a 30-volt supply.

Increasing the plate supply to 45 volts increases the gain by six decibels and provides approximately twice as much output pressure.

For methods of measuring the performance of hearing aids, reference 12 should be consulted.

Conclusion

This paper correlates the fundamental characteristics of speech and hearing which are of importance in the development and design of hearing aids. It is shown that often a particular type of selective amplification is beneficial. A novel method of obtaining this type of amplification is described. It is pointed out that certain factors external to the hearing aid determine the maximum performance obtainable. A method of computing the acoustical amplification of a hearing aid is given. Response curves of a three-tube hearing aid using a crystal-type microphone and receiver are shown.

The numerous amplifier circuits which can be used have not been discussed. These will change with time and the development of new and improved circuit components.

Future possibilities for improvement lie in the establishment of more accurate

Surface Heating by Induction

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ASSOCIATE AIEE

Synopsis: Induction heating offers a method for heating of electrical conductors such as steel, brass, graphite, by exposing them to a varying magnetic field.

The application of induction heating to melting, forging, surface hardening, and other uses is becoming increasingly common, resulting in a mounting interest for a physical concept of the heating effect itself. This effect has been previously analyzed by means of partial differential equations whose solutions are Bessel functions.[1] It is the object of this paper to analyze the induction-heating effect by using concepts commonly applied to a-c engineering without involving differential equations. It was found that ordinary vector diagrams lead to quantitative expressions for the penetration of the magnetic field into the charge, for the distribution of eddy currents and generated heat, as well as for the impedance of the loaded inductor.

The analysis is limited to the heating of a body of cylindrical shape, surrounded by a uniform alternating magnetic field; this limitation is the same as commonly applied when partial differential equations are used for the analysis.

Another limitation is introduced by the assumption that the depth of penetration is very small compared to the radius of the charge. This assumption greatly simplifies the mathematical treatment without sacrifice to the physical concept. If the depth of penetration is five per cent of the radius of the charge, the expression for the generated heat in the load will deviate only 3.6 per cent from the evaluation by Bessel functions; for smaller depths of penetration the deviation approaches zero.

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THE generation of heat within the iron core of a power transformer is termed the core loss, which is one form of induction heating. It is customary to divide the core loss into hysteresis and eddy-current loss. The hysteresis loss is a characteristic property of ferromagnetic materials, whereas the eddy-current loss is an I^2R loss due to current circulation in the material, resulting from electromotive forces induced by the varying induction. Replacing the iron of the core by a nonmagnetic metal will eliminate the hysteresis loss, but eddy currents will still be induced and heat generated. Hence, magnetic as well as nonmagnetic metals respond to induction heating.

When the frequency of the alternating magnetic flux is increased, hysteresis and eddy-current losses increase. The eddy-current loss, however, increases at a much greater rate than the hysteresis loss. At frequencies of the order of 10,000 cycles and more the eddy-current loss is predominant and the hysteresis loss is probably negligible.[2]

Among fields of application for induction heating are melting, forging, surface hardening, brazing, and soldering.[3,4,5] Induction heating is characterized by high concentration of heat per unit volume and close control of the transmitted heat. It is particularly suitable for surface hardening because the heat-producing eddy currents have a tendency to flow next to the surface of the charge. If a shaft is heated to hardening temperature at its surface zone only and is subsequently quenched, then only the surface zone hardens while its core remains soft. This distribution of hardness is often desirable. It gives the shaft a hard wear-resistant surface, whereas the core remains soft and thus avoids brittleness attendant to hardness.

The application of highly concentrated heat for surface hardening and forging shortens the heating time. This is important not only because of the saving of time in itself, but also because a short heat interval prevents or minimizes scale formation.

Although the heat is generated in the surface layer, the entire body of a charge can be heated if so desired. This is done simply by continued application of power, the heat being conducted thermally to the interior.

The Magnetic Field of the Air-Core Inductor

In order to heat a charge, it is placed inside a helical coil,[6,7] the inductor, the latter being energized from a high-frequency generator (Figure 1). Various types of high-frequency generators are on the market, such as rotary motor generators, electronic-tube oscillators, and spark-gap oscillators.[8,9,10] The frequencies for induction heating range from 60 cycles up to several megacycles. The generator energizes the inductor, thus producing an alternating magnetic flux. The latter induces electromotive forces within the charge, comparable to the induction of the voltage in the secondary of a transformer. The electromotive forces cause currents, the so-called eddy currents, to flow through the charge. The heating effect is the result of I^2R losses due to the eddy currents in combination with the electrical resistance of the charge.

It is assumed that the charge will be exposed to a uniform alternating magnetic field. Such a field is produced by a solenoid of infinite length, part of which is shown in Figure 2. The magnetic field intensity outside of the solenoid is zero; this is plausible, remembering that the return flux outside of the solenoid spreads over an infinitely large area, thus reducing the flux density to zero.[11]

Positive directions were assigned to the magnetic field intensity and the current. They are shown by arrows parallel to the axis of the inductor and by the head and tail symbols, respectively, indicating a right-hand system. For alternating magnetic intensities and currents the arrows show the direction of the instantaneous values, provided the latter appear positive in their respective vector diagrams.

For an infinitely long inductor and charge, which will be assumed in all of the following considerations, the field

methods of fitting hearing aids to the individual's specific needs and in the use of two-channel or binaural hearing aids.

References

1. RESULTS OF THE WORLD'S FAIR HEARING TESTS, J. C. Steinberg, H. C. Montgomery, M. B. Gardner. *Bell System Technical Journal*, volume 19, 1940, number 4, pages 533–62.

2. PROPOSED SPECIFICATIONS FOR AUDIOMETERS FOR GENERAL DIAGNOSTIC PURPOSES, subcommittee on audiometry and hearing aids of Acoustical Society of America. *Journal* of the Acoustical Society of America, volume 9, 1937, number 1, pages 72–3.

3. ESTIMATION OF PERCENTAGE LOSS OF HEARING, Howard A. Carter. *Journal* of the Acoustical Society of America, volume 15, 1943, number 2, pages 87–90.

4. AUDITORY PERSPECTIVE—BASIC REQUIREMENTS, Harvey Fletcher. ELECTRICAL ENGINEERING, volume 53, January 1934, pages 9–11.

5. SPEECH AND HEARING (book), Harvey Fletcher. D. Van Nostrand Company, New York, N. Y., 1929.

6. *Bell Laboratories Record*, volume 12, 1934, number 10, page 314.

7. RELATION OF AUDIOGRAM MEASUREMENTS TO HEARING-AID CHARACTERISTICS BASED ON COMMERCIAL EXPERIENCE, F. W. Kranz and Carl E. Rudiger. *Journal* of the Acoustical Society of America, volume 13, 1942, number 4, pages 363–6.

8. COMMUNICATION ENGINEERING (second edition), W. L. Everitt. McGraw-Hill Book Company, Inc., page 378.

9. PERMISSIBLE AMPLITUDE DISTORTION OF SPEECH, Frank Massa. *Proceedings* of the Institute of Radio Engineers, volume 21, 1933, number 5, pages 682–9.

10. Tucker, R. S. *Journal* of the Acoustical Society of America, volume 2, 1930, page 63.

11. United States patents 2,123,119, 2,308,930, 2,308,931, and 2,068,698, W. D. Penn.

12. METHODS FOR MEASURING THE PERFORMANCE OF HEARING AIDS, Frank Romanow. *Journal* of the Acoustical Society of America, volume 13, 1942, number 3, pages 294–304.

distribution is the same for any cross section perpendicular to its axis. It is, therefore, permissible to analyze only a limited height of inductor and charge. The magnetic-field and eddy-current distribution thus obtained will be representative for the entire height of inductor and charge. The height of the section under consideration will be limited arbitrarily to one centimeter.

The magnetic-field intensity, henceforth abbreviated magnetic intensity, follows from (see Appendix VII for symbols):

$$H = 0.4\pi In \tag{1}$$

If the current I is alternating, the magnetic intensity H is also alternating

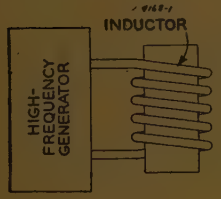


Figure 1. General arrangement of inductor and charge

and in time phase with the current I. Equation 1 can be rewritten

$$H = 0.4\pi In \tag{2}$$

The boldface letters $\boldsymbol{H}$ and $\boldsymbol{I}$ designate vector quantities. The letters H and I signify the absolute values of the vectors. The instantaneous values h and i are obtained by projecting the vectors $\boldsymbol{H}$ and $\boldsymbol{I}$, which are standing still, on the time base T (see Figure 3) which rotates clockwise with an angular velocity:

$$\omega = 2\pi f \tag{3}$$

The location of the time axis T for the time $t=0$ is coinciding with vectors $\boldsymbol{I}$ and $\boldsymbol{H}$. The angle α between $\boldsymbol{I}$ and T is

$$\alpha = \omega t \text{ in radians} \tag{4}$$

where t is the time lapse from coincidence of T with $\boldsymbol{H}$ until T reaches the position shown in Figure 3. The quantities are positive for $-\frac{\pi}{2} < \alpha < \frac{\pi}{2}$, they are negative for $\frac{\pi}{2} < \alpha < \frac{3\pi}{2}$.

Screening Effect

Suppose the inductor (Figure 4) is energized by a constant alternating current, an alternating field intensity H_0 will result, which is constant over the whole cross section of the infinitely long inductor. A thin-walled tubular metallic charge A is now inserted coaxially. The variable magnetic flux surrounded by the charge induces voltages and currents in the charge in the same manner as in the secondary of a transformer, consisting of one turn which is short-circuited.

It is convenient to assume the positive directions of both inductor and charge current in the same right-hand-system direction, as indicated by the symbols in Figure 4.

Similar to the current I in the inductor, the current I_A circulating circumferentially in the infinitely long charge produces a magnetic intensity ΔH_0 which is constant throughout the space surrounded by the charge and which is zero for any point outside. The resulting field intensity at any instant can be regarded as a result of superposing the magnetic intensities caused by the currents in inductor and charge. The magnetic field intensity H_1 in the space surrounded by the charge is

$$H_1 = H_0 + \Delta H_0 \tag{5}$$

where H_0 is the magnetic intensity produced by the inductor current alone and

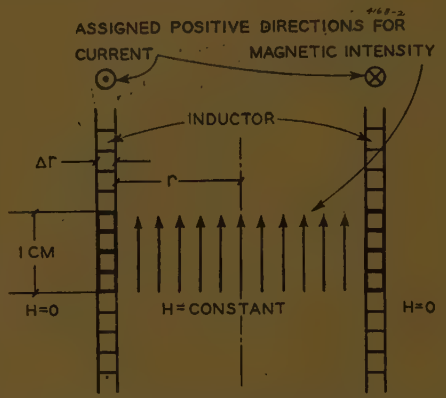


Figure 2. Magnetic intensities of infinitely long empty air-core inductor

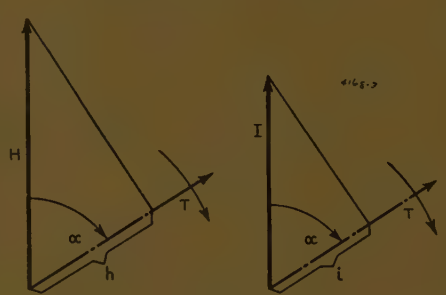


Figure 3. Vector diagrams in general

ΔH_0 the magnetic intensity produced by the charge current alone. In analogy to equation 1 and for $n = 1$

$$\Delta H_0 = 0.4\pi I_A \tag{6}$$

The induction B_1 in the space surrounded by the charge is

$$B_1 = \mu H_1 \tag{7}$$

and the magnetic flux Φ_1 surrounded by the charge is

$$\Phi_1 = B_1 a^2 \pi = \mu H_1 a^2 \pi \tag{8}$$

Φ_1 and H_1 are parallel vectors. The electromotive force E_A generated in the charge A by flux Φ_1 is lagging 90 degrees behind it (Figure 5). Overcoming the resistance of the current path, E_A causes the flow of current I_A, which is in phase with E_A. ΔH_0 is parallel to I_A. H_0 follows from equation 5 as $0P$. The flux Φ_0 between inductor and charge is in phase with H_0.

An inspection of the vector diagram of Figure 5 reveals:

1. The magnetic intensity H_0 outside the charge is reduced in intensity to H_1 as it penetrates the charge. This effect which is due to the circulation of eddy currents in the charge is often called screening effect.

2. The currents in the charge cause a time lag between the magnetic intensities, resulting in a lag of the flux Φ_1 surrounded by the charge, with respect to the flux Φ_0 outside of the charge.

Solid Cylindrical Charge

General Considerations

A solid charge can be considered made up by a large number of thin-walled tubular sleeves which are telescoped into each other; the eddy-current heating of a solid charge will be approached by considering the current distribution in the individual thin-walled sleeves, reducing the thickness of the latter toward zero.

Judging from the case of a single thin-walled sleeve, one is led to expect that the magnetic intensity H_0 will be reduced a certain percentage while penetrating the sleeve S_1 (Figure 6) and that it will emerge from sleeve S_1 as the intensity H_1; it can also be expected that the emerging field intensity H_1 will lag behind the entering intensity H_0. The vectors of the magnetic intensities are shown in larger scale in Figure 7. The

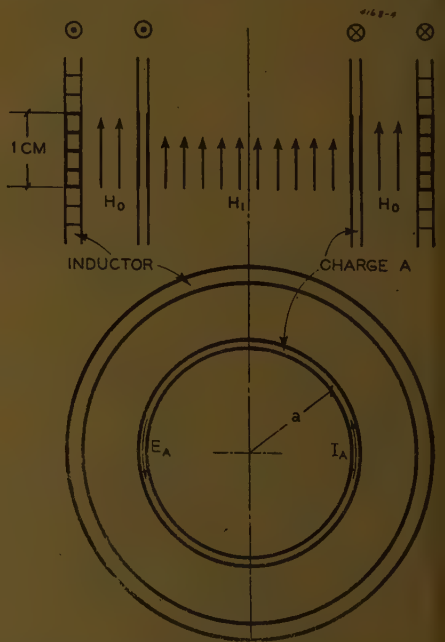


Figure 4. Field distribution in inductor containing a tubular charge of small wall thickness

angle $01P$ between ΔH_0 and H_1 (Figure 7), however, cannot be expected to remain 90 degrees as in the case of Figure 5, because the current I_I flowing in sleeve S_I is no longer determined by the electromagnetic action of sleeve S_I only, but also by the electromagnetic action of all the other sleeves S_{II}, S_{III}. . . . . inside of S_I. The current I_I in S_I is a function of the total flux surrounded by S_I, which in turn is a function of the different currents in all the sleeves. All that can be expected is that the magnetic intensity H_0, by penetrating sleeve S_I of the thickness Δr, will emerge as the intensity H_1, lagging $\Delta\alpha$ behind H_0, and being reduced in magnitude by a definite percentage of its entering value. The magnetic intensity H_1 then enters sleeve S_{II}. If the wall thickness is the same for S_I and S_{II}, it can be assumed that the emerging magnetic intensity H_2 will lag by the same angle $\Delta\alpha$ behind H_1 as H_1 was lagging behind H_0 in sleeve S_I. The ratio of reduction of H_1 to H_2 can be assumed to be equal to H_0 to H_1. These assumptions are justified by leading to interdependent systems of magnetic intensities and fluxes, electromotive forces, and currents, which fulfill all physical requirements.

Vector Diagram of Magnetic Intensities

A vector diagram embodying the foregoing assumptions is shown in Figure 7. Let

$$H_1/H_0 = H_2/H_1 = \ldots\ldots H_m/H_{m-1} = q = \text{constant} \quad (9)$$

$$\sphericalangle 0P1 = 1P2 = 2P3 = \ldots\ldots = \Delta\alpha = \text{constant}$$

It follows that the triangles $P01$, $P12$, $P23$,..... are similar (two homologous sides and one included angle $\Delta\alpha$). These geometric relations are mathematically expressed by

$$H_m = H_0 \epsilon^{-(m\Delta\alpha)\cot\beta} \qquad \alpha \text{ in radians} \quad (10)$$

as shown in Appendix I. If the phase angle of H_m with respect to H_0 is α, then

$$\alpha = m\,\Delta\alpha \quad (11)$$

and

$$H_m = H_0 \epsilon^{-\alpha\cot\beta} \qquad \alpha \text{ in radians} \quad (12)$$

indicating a logarithmic spiral as locus for the magnetic intensity.

Vector Diagram of Magnetic Fluxes

The flux Φ_I contained within the body of sleeve S_1 is

$$\Phi_I = B_I \text{ area} = \mu H_I[(\Delta r/2)(a - \Delta r/2)\pi] \quad (13a)$$

likewise

$$\Phi_M = \mu H_M\left[(\Delta r/2)\left(a - \frac{2m-1}{2}\Delta r\right)\pi\right] \quad (13m)$$

Subscripts I and M refer to the quantities existing in the middle of the body of the first and mth sleeve, respectively, whereas subscripts 1 and m refer to the quantities at the lateral inner surface of the first and mth sleeve, respectively.

It can be seen from Figure 7 that the magnetic intensities and thus the inductions decline very rapidly as the magnetic field penetrates into the charge. The variation of the areas over which the fluxes are spread can be neglected if the magnetic intensity rapidly decreases as it penetrates the charge. Thus

$$\Phi_I = H_I \mu\, \Delta r 2a\pi \quad (14a)$$

$$\Phi_{II} = H_{II} \mu\, \Delta r 2a\pi \quad (14b)$$

$$\Phi_M = H_M \mu\, \Delta r 2a\pi \quad (14m)$$

Adding the fluxes Φ_I, Φ_{II}, Φ_{III} ..... vectorially (by using equations 14a, 14b, and 14m in combination with Figure 7), the vector sum follows a logarithmic spiral which is similar to the locus for the magnetic intensity as proved in Appendix II. The spiral for the fluxes is shown in Figure 8.

The total magnetic flux Φ inside of the charge is the vector sum of all individual fluxes Φ_I, Φ_{II}, Φ_{III}..... For a sleeve of infinitesimal thickness the angle $\Delta\alpha = 0$ and the phase angle between total flux Φ inside the entire charge and the magnetic intensity H_0 between inductor and charge is

$$\beta = 45 \text{ degrees} \quad (21)$$

Since inductor current I and H_0 are in phase, the angle between I and Φ is also 45 degrees (Figure 9).

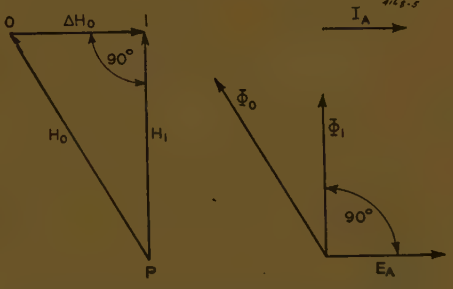


Figure 5. Vector diagram for inductor with tubular charge of small wall thickness

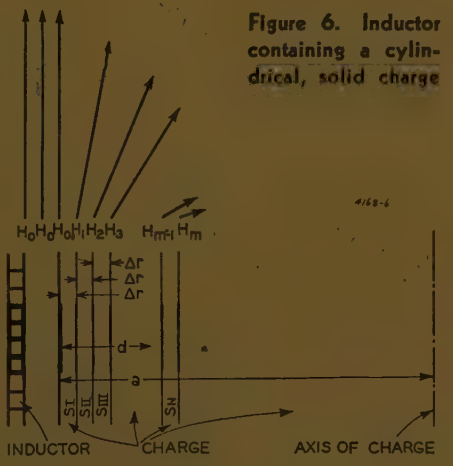


Figure 6. Inductor containing a cylindrical, solid charge

Power Factor of Charge

The total flux Φ induces a voltage E in the inductor which is 90 degrees lagging behind Φ and 45 additional degrees behind the inductor current I. Voltage E consists of two components E_R and E_X of equal magnitude. E_R is in counterphase with I and reflects the watt power consumed by the charge. E_X is lagging 90 degrees behind the current and indicates that the charge consumes inductive power as well. Due to the equality in magnitude of E_R and E_X, the load consumes as much watt power as wattless inductive power. Therefore,

Power factor of charge = cos 45 degrees
= 0.707 inductive (22)

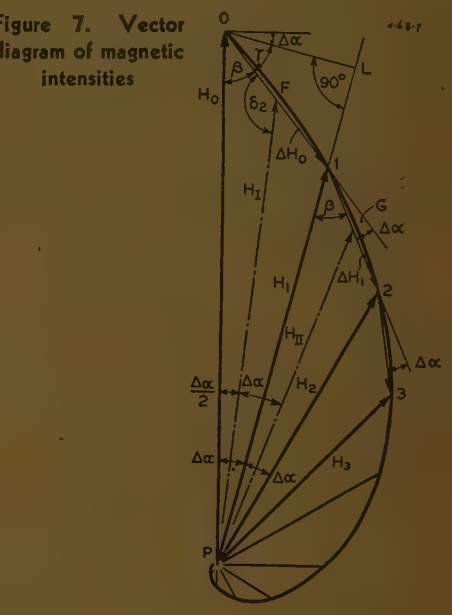


Figure 7. Vector diagram of magnetic intensities

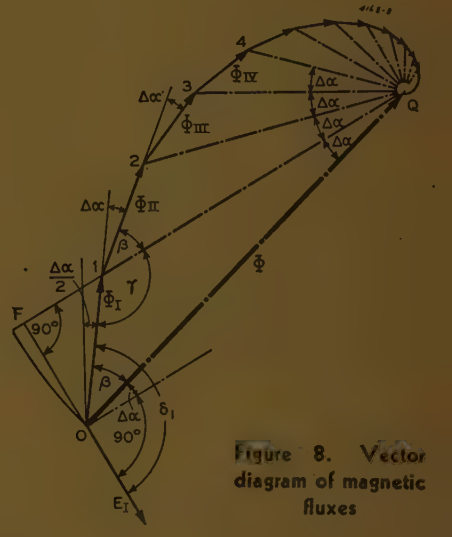


Figure 8. Vector diagram of magnetic fluxes

Penetration of the Magnetic Field

The total magnetic flux Φ within the boundaries of the charge is evaluated in Appendix III:

$$\Phi = B_0(2a\pi)p \quad (30)$$

where B_0 is the induction in the space between inductor and charge. If the

induction B_0 would penetrate into the charge to a depth of p centimeters, without being attenuated, the magnitude of the flux thus obtained would be equal to the magnitude of the actual flux Φ. For this reason the factor p is often called equivalent depth of the magnetic-field penetration, or shortly penetration. It follows from Appendix III:

$$p=\frac{1}{2\pi}\sqrt{\frac{\rho 10^9}{2\mu f}} \qquad (31)$$

The graph shown in Figure 10 serves for quick determination of the penetration p. Suppose the penetration should be found for soft iron (resistivity $\rho=10^{-5}$ ohm-centimeter, permeability $\mu=1{,}000$) at a frequency of one kilocycle. Intersection of the frequency and resistivity

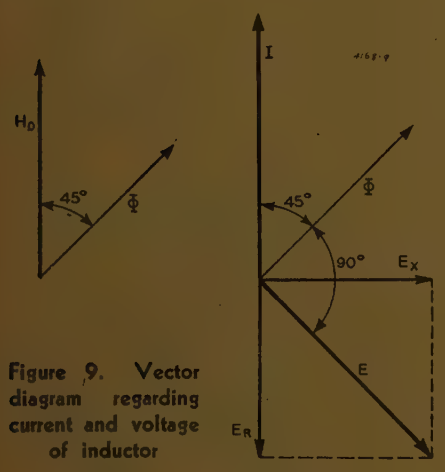


Figure 9. Vector diagram regarding current and voltage of inductor

co-ordinates occurs at point A. From A one follows the oblique line until it intersects with the permeability system at point B. Projecting point B on the penetration axis gives the penetration, which in this case is slightly over 10^{-2} centimeter. The magnetic intensity H_d at a distance d centimeters from the surface is

$$H_d=H_0\epsilon^{-\frac{d}{p\sqrt{2}}} \qquad (35)$$

and the phase angle α_d between H_d and H_0

$$\alpha_d=d/(p\sqrt{2}) \text{ in radians} \qquad (36)$$

Field Propagation Inside the Charge

Returning to the vector diagram of Figure 7, the various vectors H_0, H_1, H_2 represent the magnetic intensities at definite distances from the surface of the charge. These distances d follow from equation 36. In the vector diagram of Figure 7 the angle $\Delta\alpha$ was made 15 degrees, which is identical with $\pi/12$ radians. The distance at which vector H_1 represents the magnetic intensity can be obtained from equation 36:

$$d=\alpha_d p\sqrt{2}=(\pi/12)p\sqrt{2} \text{ in centimeters}$$

Conversely, the magnitude and phase of the magnetic field for any given distance d from the surface can be obtained by computing α_d from equation 36, and subtending α_d from H_0 to intersect the logarithmic spiral.

Using the vector diagram of Figure 7, a graph of the instantaneous distribution of magnetic intensities can be plotted (Figure 11). The plane of the graph can be considered as a partial lateral section through the cylindrical charge. The graph is limited on the left-hand side by the surface contour of the charge. The axis of the charge is parallel to the contour off to the right. The abscissa axis can be viewed as one radius of the charge. In order to generalize the graph, relative values $d/(p\sqrt{2})$ and h/H_0 were

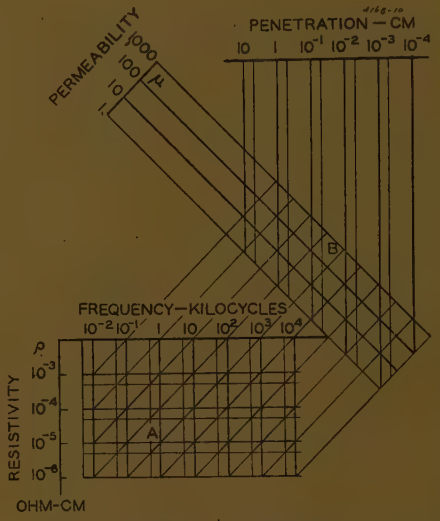


Figure 10. Graph for quick determination of the penetration p

chosen as scales for the axes. The distance d is also marked for multiples of the penetration p.

The instantaneous values h of the magnetic intensities are obtained by projecting the vectors H (which are standing still) on a common time axis T. The time axis rotates clockwise and makes one revolution during each cycle of the high-frequency current (equation 3). For $t=0$, the time axis coincides with H_0. After one eighth of a cycle the time axis includes an angle of 45 degrees with vector H_0 and the instantaneous values h at this time instant are obtained by projecting all vectors H on the time axis, now 45 degrees behind H_0. Curve A was thus obtained (Figure 11). The remaining curves B, C, and D, were obtained in the same manner, the time interval being always one eighth of a cycle. Each curve has crest points and zero points. Observing these points from curve to curve, it looks as though these points, and the parts of the curves in between, travel from the surface of the charge toward the interior, as time goes on. It will also be observed that the height of the wave decreases as it penetrates the charge. It is therefore possible to view the field distribution inside of the charge as the result of an electromagnetic wave entering the charge at its surface and being attenuated as it proceeds.

Density of Eddy Currents

The current density K_0 in the surface layer follows from Appendix IV.

$$K_0=In/p \text{ amperes per square centimeter} \qquad (37)$$

In general, the current density depends on the flux with which the current filament is interlinked and the resistance of the path; the latter is assumed constant. Hence the current densities can be expressed by the same exponential function as the interlinking fluxes and the magnetic intensities, respectively (equations 12 and 21). The current density K_d at the distance d from the surface

$$K_d=K_0\epsilon^{-\frac{d}{p\sqrt{2}}}=(In/p)\epsilon^{-\frac{d}{p\sqrt{2}}} \qquad (37a)$$

The generation of heat per unit volume at any depth d is:

$$w_d=\tfrac{1}{2}K_d^2\rho=\tfrac{1}{2}(In/p)^2\rho\epsilon^{-\frac{d\sqrt{2}}{p}} \text{ watts per cubic centimeter} \qquad (37b)$$

Plotting the function $\epsilon^{-\frac{d\sqrt{2}}{p}}$ in Figure 12 discloses the rapid reduction of heat generation with increasing distance from the surface. The concentration of the eddy currents and hence the generation of heat next to the surface of the charge is called skin effect. The heat generated in a zone reaching to a depth of $3p$ is 98.5 per cent of the total generated heat.

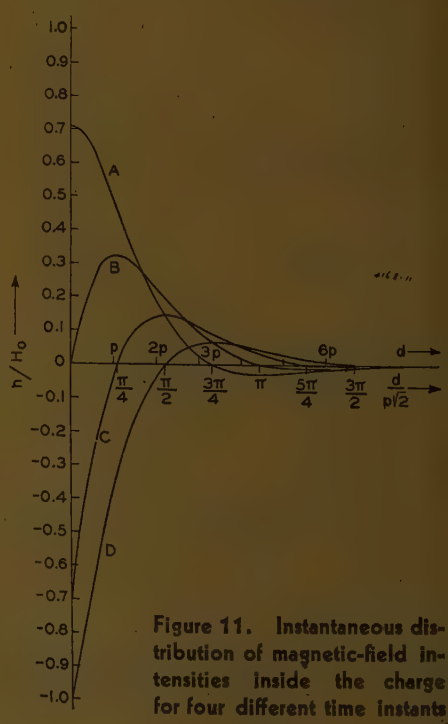


Figure 11. Instantaneous distribution of magnetic-field intensities inside the charge for four different time instants

Since the penetration p depends on the frequency of the inductor current (equation 31) one can vary the thickness of the heated zone by controlling the frequency. The case thickness can be reduced by increasing the frequency, conversely lowering the frequency will increase the depth of the heated zone as it is desirable in forging and melting applications.

Power Transfer and Impedance of Loaded Inductor

The power P delivered to the charge per centimeter axial length is shown in Appendix V.

$$P = 4\pi^2 a\,(I_{eff} n)^2 \sqrt{\mu f \rho\, 10^9}\, 10^{-9} \text{ watts per centimeter axial length of charge} \quad (38)$$

All other conditions remaining the same, increasing the frequency increases the rate of heating. A material with greater resistivity will generate more heat, provided the depth of penetration is much smaller than the radius of the charge (see synopsis). Moreover, a material with a positive temperature-resistivity coefficient, will generate heat in increasing measure as it warms up.

Due to permeability, ferromagnetic materials will heat more rapidly than nonferrous materials of the same resistivity. However, when the critical temperature is reached ferromagnetism disappears and the permeability drops to unity. Thus, the heating of ferromagnetic materials is greatly reduced when the critical temperature is obtained.

As previously pointed out, the charge consumes watt power and a numerically equal amount of wattless inductive power. The inductor presents itself with a certain impedance, which is called the referred impedance Z_a of the load (charge); in addition the inductor will have a self-impedance Z_b consisting of the high-frequency impedance of its own winding and a reactance due to the magnetic field in the interspace between inductor and charge.

The above considerations lead to the expression for the referred resistance R_a of the load, as shown in Appendix VI.

$$R_a = 4\pi^2 a n^2 \sqrt{\mu \rho f\, 10^9}\, 10^{-9} \text{ ohms per centimeter axial length of charge} \quad (39)$$

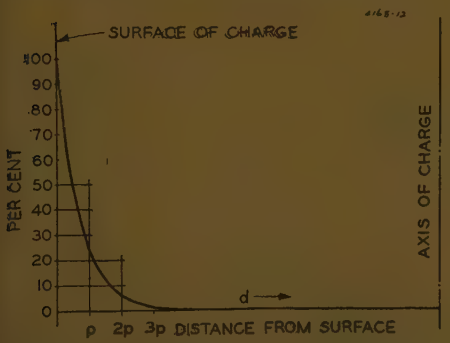


Figure 12. Relative heat generation as a function of the distance d from the surface

Since watt and wattless power are numerically equal, the referred reactance

$$|X_a| = R_a$$

and the referred impedance

$$Z_a = R_a + jX_a = R_a(1+j)$$

The high-frequency resistance R_b of the inductor can be approximated by using equation 39. For a water-cooled copper inductor the resistance R_b is according to Appendix VI:

$$R_b = 1{,}8\, b\sqrt{f}\, n^2 (c_1/c_2) 10^{-9} \text{ ohms per centimeter axial length of charge} \quad (40)$$

The losses in the inductor increase with its radius b. In order to obtain high efficiency of power transfer, the radius of the inductor should not be larger than necessary. Hence, the clearance between inductor and charge should be kept to a minimum.

Since watt and wattless power are numerically equal, the referred reactance of the inductor due to the field within the boundaries of the conductors is:

$$|X_b| = R_b$$

Hence the impedance

$$Z_b = R_b + jX_b = R_b(1+j)$$

The reactance X_0 due to the magnetic field in the interspace between inductor and charge is shown in Appendix VI.

$$X_0 = 8\pi^3 f\,(b^2 - a^2) n^2\, 10^{-9} \text{ ohms per centimeter axial length of charge} \quad (41)$$

The total impedance Z of the loaded inductor is therefore:

$$Z = (R_a + R_b) + j(R_a + R_b + X_0) \text{ ohms per centimeter axial length of charge} \quad (42)$$

Appendix I

The ratio of two consecutive magnetic intensities from equation 10 is

$$H_m = H_0 \epsilon^{-m\Delta\alpha \cot\beta}$$

$$H_{m-1} = H_0 \epsilon^{-(m-1)\Delta\alpha \cot\beta}$$

Therefore:

$$H_m / H_{m-1} = \epsilon^{-\Delta\alpha \cot\beta} = q \quad (10a)$$

which is constant for positive, integral m.

$$q = H_1/H_0 = (\sin\beta)/\sin(\beta+\Delta\alpha)$$

$$= 1 - \frac{\sin(\beta+\Delta\alpha) - \sin\beta}{\sin(\beta+\Delta\alpha)}$$

$$= 1 - \frac{2\cos(\beta+\Delta\alpha/2)\sin(\Delta\alpha/2)}{\sin(\beta+\Delta\alpha)}$$

For sleeves of infinitesimal wall thickness

$$\sin\Delta\alpha/2 = \Delta\alpha/2 \quad (10b)$$

and

$$\beta + \Delta\alpha/2 = \beta + \Delta\alpha = \beta \quad (10c)$$

hence

$$q = 1 - \Delta\alpha \cot\beta \quad (10d)$$

The same result is obtained by using equations 10 and 10a:

$$\epsilon^{-\Delta\alpha\cot\beta} = 1 - \Delta\alpha\cot\beta \text{ for } \Delta\alpha\cot\beta \ll 1$$

Appendix II

Vector 01 representing Φ_I (Figure 8) is parallel to $\boldsymbol{H}_I$ of Figure 7 and determined by equation 14a. Vector 12 is parallel to $\boldsymbol{H}_{II}$ and determined by equation 14b. Hence

$$\Phi_{II}/\Phi_I = H_{II}/H_I \quad (15)$$

$\boldsymbol{H}_I$ connects midpoint F of 01 (Figure 7) to P; $\boldsymbol{H}_{II}$ was obtained likewise. Triangles $0PF$ and $1PG$ are similar (two homologous sides and one included angle β) and hence:

$$H_{II}/H_I = H_I/H_0 \quad (16)$$

From equations 9, 15, and 16:

$$\Phi_{II} = \Phi_I H_{II}/H_I = \Phi_I H_I/H_0 = \Phi_I q \quad (17)$$

Point Q (Figure 8) was obtained by subtending the angle β from Φ_I and Φ_{II} (Figure 8).

$$\gamma = 180° - \Delta\alpha - \beta$$

$$\sphericalangle 1Q0 = 180 - \beta - \gamma = 180 - \beta - 180 + \Delta\alpha + \beta = \Delta\alpha° \quad (18)$$

Triangles $01Q$, $12Q$, $01P$, and $12P$ are similar. If another similar triangle $23Q$ is based on $2Q$, it can be shown in the same manner that 23 represents vector Φ_{III}. The sum of the first three individual fluxes Φ_I, Φ_{II}, Φ_{III} is $Q3$. The remaining individual fluxes and their sum can be determined by a repetition of the same procedure. The triangles become progressively smaller and finally degenerate in point Q. Line $0Q$ therefore represents the vector sum of all fluxes, and is denoted Φ. It is the total magnetic flux within the boundaries of the charge. Due to the similarity of all triangles involved, curve $01234\ldots P$ (Figure 7) is similar to $01234\ldots Q$ (Figure 8).

The electromotive force $\boldsymbol{E}_I$ induced in sleeve S_I is due to the action of all the fluxes Φ_{II}, $\Phi_{III}\ldots\ldots$, surrounded by sleeve S_I. The vector sum of these fluxes is $1Q$ (Figure 8). $\boldsymbol{E}_I$ is lagging 90 degrees behind $1Q$. Angle δ_1 between $\boldsymbol{E}_I$ and Φ_I is

$$\delta_1 = \beta + \Delta\alpha + 90° \quad (19)$$

Angle δ_2 between $\Delta\boldsymbol{H}_0$ and $\boldsymbol{H}_I$ (Figure 7) can be expressed

$$\delta_2 = 180° - \beta - \Delta\alpha/2 \quad (20)$$

$\Delta\boldsymbol{H}_0$ is in phase with $\boldsymbol{I}_I$ (equation 6) and $\boldsymbol{H}_I$ is in phase with Φ_I, equation 14a, so that δ_2 is also the angle between Φ_I and $\boldsymbol{I}$. Remembering that $\boldsymbol{I}_I$ and $\boldsymbol{E}_I$ are in phase, angle δ_2 is the angle between Φ_I and $\boldsymbol{E}_I$ as well. This angle has been previously denoted with δ_1. Therefore from equations 19 and 20:

$$\delta_1 = \delta_2$$

$$\beta + \Delta\alpha + 90° = 180 - \beta - \Delta\alpha/2$$

$$\beta = 45° - 3\Delta\alpha/4$$

For infinitesimal thickness of the sleeve, $\Delta\alpha$ disappears and

$$\beta = 45 \text{ degrees} \qquad (21)$$

Appendix III

The electromotive force E_0 in the surface zone of the charge is a function of the total flux Φ:

$$e_0 = -\frac{d\varphi}{dt}\, 10^{-8}$$

hence the crest value

$$E_0 = 2\pi f \Phi 10^{-8} \qquad (23)$$

The current I_0 in the surface layer

$$I_0 = E_0/R_0 \qquad (24)$$

where R_0 is the resistance of the surface layer per centimeter axial length of the charge

$$R_0 = \frac{2a\pi}{\Delta r \cdot 1\ cm}\rho \qquad (25)$$

From equations 23, 24, and 25,

$$I_0 = 2\pi f \Phi 10^{-8}\frac{\Delta r}{2a\pi\rho} = \frac{\Delta r f}{a\ \rho}\Phi 10^{-8} \qquad (26)$$

$$\Delta H_0 = 0.4\pi I_0 = 4\pi(f/\rho)(\Delta r/a)\Phi 10^{-9} \qquad (27)$$

Because of the similarity of triangles $0P1$ (Figure 7) and $0Q1$ (Figure 8)

$$\Phi/\Phi_1 = H_0/\Delta H_0 \qquad (28)$$

For infinitesimal sleeve thickness $H_0 = H_I$, moreover, substituting for H_I from equation 14a and for ΔH_0 from equation 27, one obtains from equation 28

$$\frac{\Phi}{\Phi_1} = \frac{\Phi_1/\mu\,\Delta r 2a\pi}{4\pi(f/\rho)(\Delta r/a)\Phi 10^{-9}} = \frac{1}{\Delta r}\sqrt{\frac{1}{8\pi^2}\frac{\rho 10^9}{\mu f}} \qquad (29)$$

Substituting for Φ_1 from equation 14a and for $B_1 = B_0$:

$$\Phi = B_0 2a\pi\left(\frac{1}{2\pi}\sqrt{\frac{\rho 10^9}{2\mu f}}\right) = B_0 2a\pi p \qquad (30)$$

where

$$p = \frac{1}{2\pi}\sqrt{\frac{\rho 10^9}{2\mu f}} \qquad (31)$$

Figure 8 reveals that

$$\Phi_1/\Phi = (\sin\ \Delta\alpha)/\sin\ (\beta + \Delta\alpha)$$

Considering equations 10b, 10c, and 21,

$$\Phi_1/\Phi = \Delta\alpha\sqrt{2}$$

which combined with equation 29, gives

$$\Phi_1/\Phi = \Delta\alpha\sqrt{2} = \Delta r/p \qquad (32)$$

The magnetic field by penetrating sleeve by sleeve accumulates for each sleeve thickness Δr a phase displacement of $\Delta\alpha$. Penetrating m sleeves

$$d = m\,\Delta r \qquad (33)$$

The phase angle α follows from equation 11

$$\alpha = m\,\Delta\alpha$$

Using equations 11 and 33 in equation 32,

$$\Delta r/\Delta\alpha = d/\alpha = p\sqrt{2} \qquad (34)$$

Substituting equation 34 in equations 12 and 21 and using the subscript d instead of m, indicating the magnetic intensity at the distance d from the surface,

$$H_d = H_0\epsilon^{-\frac{d}{p\sqrt{2}}} \qquad (35)$$

and the phase angle α_d between H_d and H_0 from equation 34

$$\alpha_d = \frac{d}{p\sqrt{2}} \text{ in radians} \qquad (36)$$

Appendix IV

The current density K_0 in the surface layer

$K_0 = I_0/\text{cross section of path}$

Using equation 26 and substituting for Φ from equations 30 and 7,

$$K_0 = \frac{(f/\rho)(\Delta r/a)\mu H_0 2a\pi p\ 10^{-8}}{\Delta r \cdot 1cm}$$

$$= \frac{10}{4\pi} H_0 4\pi^2 \frac{2\mu f}{\rho}\, p\ 10^{-9}$$

Substituting from equation 31 for p,

$$K_0 = H_0(10/4\pi)/p = (4\pi/10)In(10/4\pi)/p = In/p \qquad (37)$$

Appendix V

Assigning the same radius a to the various sleeves of infinitesimal thickness (which is permissible without undue error for $p < a/20$), the total heat W:

$$W = 2a\pi\int_{d=0}^{a} w_d d(d) = (In/p)^2\rho\pi a \times \int_{d=0}^{a} \epsilon^{-\frac{d\sqrt{2}}{p}}\, d(d)$$

$$W = (In/p)^2\rho\pi(-ap/\sqrt{2})\left(\epsilon^{-\frac{a}{p}\sqrt{2}} - 1\right)$$

For $p < a/20$ $\quad \epsilon^{-\frac{a}{p}\sqrt{2}} \approx 0$

$$W = (In/p)^2\rho\pi ap/\sqrt{2} = 4\pi^2 a(I_{eff}n)^2 \times \sqrt{\mu f\rho\ 10^9}\ 10^{-9} \qquad (38)$$

Appendix VI

The referred resistance R_a follows from equation 38:

$$I_{eff}^2 R_a = 4\pi^2 a(I_{eff}n)^2\sqrt{\mu f\rho\ 10^9}\ 10^{-9}$$

$$R_a = 4\pi^2 an^2\sqrt{\mu f\rho\ 10^9}\ 10^{-9} \text{ ohms} \qquad (39)$$

For a copper inductor, using a rectangular water-cooled conductor, $\mu = 1$; the resistivity of the inductor at a temperature of 60 degrees centigrade $\rho = 2.04\ 10^{-6}$ ohm-centimeter, using these values in equation 39:

$$R_b = 1.8\ b\ n^2\sqrt{f}\ (c_1/c_2)\ 10^{-6} \qquad (40)$$

The flux Φ_0 in the interspace is:

$$\Phi_0 = 0.4\pi In(b^2 - a^2)\pi,$$

$$e = -\frac{d\varphi_0}{dt}\, n10^{-8} = -0.4\pi n^2(b^2 - a^2)\pi\frac{di}{dt}\, 10^{-8}$$

$$E = 8\pi^3 n^2 f(b^2 - a^2) I\ 10^{-9} \text{ volts}$$

$$X_0 = E/I = 8\pi^3 n^2 f(b^2 - a^2)10^{-9} \text{ ohms} \qquad (41)$$

Appendix VII. Definitions of Symbols

a—radius of the charge, centimeters
B—induction, peak value of sinusoid, gausses
b—radius of the inside of the inductor, centimeters
c_1—winding pitch of the inductor, centimeters
c_2—height of the conductor, centimeters
d—distance of a point inside of charge to surface, centimeters
E—electromotive force, peak value of sinusoid, volts
e—electromotive force, instantaneous value, volts
f—frequency of inductor current, cycles per second
H—magnetic intensity, peak value of sinusoid, oersteds
h—magnetic intensity, instantaneous value, oersteds
I—current, peak value of sinusoid, amperes
I_{eff}—current, rms value, amperes
i—current, instantaneous value, amperes
j—$\sqrt{-1}$
K—current density, peak value of sinusoid, amperes per square centimeter
n—number of turns of inductor per centimeter axial length
P—power, average value, watts
p—penetration, centimeters
Δr—thickness of sleeve, centimeters
R_a—referred resistance of charge, ohms per centimeter axial length
R_b—high-frequency resistance of inductor, ohms per centimeter axial length of inductor
t—time, seconds
w—generated heat, average value, watts per cubic centimeter
X_0—reactance of inductor due to the magnetic field in the interspace between inductor and charge, ohms per centimeter axial length of inductor
Z—total impedance of loaded inductor, ohms per centimeter axial length of inductor and charge
$\alpha, \beta, \gamma, \delta$—angles, in degrees or radians, as noted
ϵ—base of natural logarithms, 2.71828
μ—permeability, unity
π—3.14159
Φ—magnetic flux, peak value of sinusoid, maxwells
φ—magnetic flux, instantaneous value, maxwells
ρ—resistivity, ohm-centimeters
ω—angular velocity, radians per second

References

1. Bessel Functions for Engineers (book), N. W. M'Lachlan. Clarendon Press, Oxford, England, 1934.

2. Surface Hardening by Induction, H. B. Osborn, Jr. Electrochemical Society *Transactions*, volume 79, April 1941, pages 16–19.

Induction Heating—Selection of Frequency

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MEMBER AIEE

THE general definition of induction heating is the use of eddy currents to raise the temperature of a conducting mass. Thus induction heating is an application of the theory of the eddy-current phenomenon.

This paper deals with the relation of frequency and kilovolt-ampere capacity as the basis for the selection of the frequency for a given heating service.

Charges. The body (or bodies collectively) to be heated is termed the charge. Charges in induction-heating practice are of three types, namely:

1. Charges of nonmagnetic material.

2. Charges of magnetic material. Maximum temperature below the critical temperature.

3. Charges of magnetic material. Maximum temperature above the critical temperature.

The Solid Cylindrical Charge. The shape of the charge is taken into account in analyses of the heating effect of eddy currents. However, the shape of the charge is not a factor in the principle involved. Hence a simple shape, the solid cylinder, is selected as the basis of this discussion.

The Transformer. The most effective arrangement of parts for induction heating is the charge inside a coil. An assembly of this type, less supports, for heating a charge to a temperature below the melting point of the material is shown in Figure 1.

This assembly is a form of transformer with these peculiar features:

(*a*). A closed secondary circuit formed in the charge.

(*b*). An open magnetic circuit. In some cases iron is added to the portion of the path of the magnetic flux that is outside of the charge. However, the general case is expressed by the term "coreless induction heating."

(*c*). The air gap between the primary and secondary circuits.

Circuit Equations. The equations of the circuits of the transformers previously noted are given in the appendix.

Basic Equations. The relation of the factors in the eddy-current phenomenon is given in the three equations which follow.

Reference dimension,

$$p=\frac{\sqrt{\rho 10^9}}{2\pi\sqrt{2}\sqrt{\mu f}}\ \text{centimeters} \qquad (1)$$

$$=\frac{3{,}560\sqrt{\rho}}{\sqrt{\mu f}}\ \text{centimeters} \qquad (1a)$$

$$\text{Index ratio, } \Delta=\frac{a}{p} \qquad (2)$$

p = the radial dimension of the annular zone of the charge indicated in Figure 2

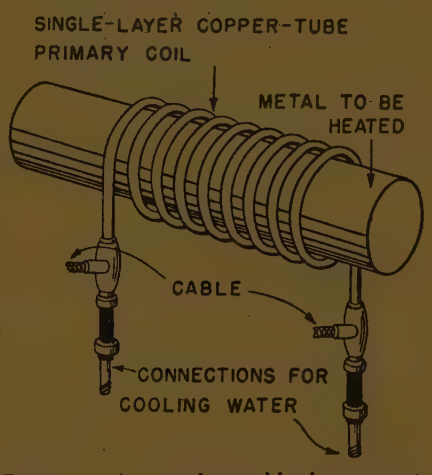


Figure 1. A typical assembly, less supports, for heating solids

ρ = the resistivity, ohm-centimeters[2], of the material of the charge

μ = the permeability of the material of the charge

f = frequency

a = the radius of the charge, centimeters

Rate of Heat Development. The rate at which heat is developed by eddy currents in a solid cylindrical charge is expressed by the equation:

$$P_{cm}{}^3=\left(\frac{1}{4\pi\sqrt{2}}\right)(H^2)\left(\frac{\sqrt{\mu f}\sqrt{\rho 10^9}}{a}\right)\times [F_r]\times 10^{-7} \qquad (3)$$

$P_{cm}{}^3$ = watts per cubic centimeter

H = the intensity of the alternating magnetic flux at the peripheral surface of the charge

$$[F_r]=\left[\frac{\text{ber}\,\Delta\,\text{ber}'\,\Delta+\text{bei}\,\Delta\,\text{bei}'\,\Delta}{\text{ber}^2\,\Delta+\text{bei}^2\,\Delta}\right]$$

Values of $[F_r]$ for a range of values of the index ratio are given in Figure 3.

Resistivity. The resistivities of metals have positive temperature coefficients. This characteristic is illustrated by Figure 4 for iron and carbon steel in the solid state. There is a marked increase of resistivity at the melting points of metals. The resistivities of steels in the molten state are within the range 150 to 200 microhm-centimeters cube. The value in each case depends on the analysis of the steel and its temperature.

The value of ρ to insert in equations 1, 2, and 3 is the value that corresponds to the maximum temperature of the charge.

Permeability. Values of μ are based on uniform distribution of magnetic flux. A feature of induction heating is nonuniformity of flux distribution. This and the fact that with alternating current the permeability changes during every cycle make uncertain the effective value of μ to insert in equations 1, 2, and 3 for type-2 charges. Generally it is assumed that the effective value of μ is some fraction of the saturation value. In any case the value of μ for calculations must be either estimated or determined by measurements.

The effect of the magnetic property in the batch heating of type-3 charges is an initial boosting of the rate of heat development. This effect lasts until the outer layer of the charge reaches the critical temperature. Important as this effect may be, it is not necessary to consider it in circuit calculations. Thus the value $\mu=1.0$ is used in equations 1 and 3. It is stated in a later paragraph that

3. HEATING BY HIGH-FREQUENCY INDUCTION, F. T. Chestnut. *Westinghouse Engineer*, volume 2, February 1942, pages 11–13.

4. EXTERNAL SURFACE HARDENING, W. E. Benninghoff, H. B. Osborn, Jr. *Westinghouse Engineer*, volume 2, February 1942, pages 14–16.

5. INTERNAL SURFACE HARDENING, H. E. Somes. *Westinghouse Engineer*, volume 2, February 1942, pages 17–19.

6. CONSTRUCTION OF HEATING COIL FOR INDUCTION SURFACE HARDENING, G. Babat. *Heat Treating and Forging*, volume 27, 1941, pages 39–40, 89–92, 137–9, 192–4.

7. HEAT TREATMENT BY HIGH-FREQUENCY INDUCTION, L. C. Conradi. *Metal Progress*, volume 34, 1938, number 3, pages 214–17.

8. APPLICATION OF VACUUM-TUBE OSCILLATORS TO INDUCTIVE AND DIELECTRIC HEATING IN INDUSTRY, J. P. Jordan. AIEE TRANSACTIONS, volume 61, 1942, November section, pages 831–4.

9. ELECTRICAL EQUIPMENT FOR INDUCTION HEATING, C. C. Levy, L. J. Lunas. *Westinghouse Engineer*, volume 2, February 1942, pages 20–2.

10. DEVELOPING AN INDUCTION-HEATING MACHINE, W. Tuerck. *Machine Design*, January 1944, page 137.

11. PRINCIPLES OF D-C MACHINES (fifth edition), A. S. Langsdorf. McGraw-Hill Book Company, Inc., New York, N. Y., 1940, page 119.

Paper 44-166, recommended by the AIEE committee on electrochemistry and electrometallurgy for presentation at the AIEE summer technical meeting, St. Louis, Mo., June 26–30, 1944. Manuscript submitted April 17, 1944; made available for printing May 17, 1944.

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the boosting effect previously noted may be given qualitative consideration with reference to the selection of frequency.

With the continuous-heating method for type-3 charges the effect of permeability on the rate of heat development is continuous but less in degree than in the batch heating of this type of charge. As with type-2 charges, the effective value of μ must be estimated or obtained by measurement.

Operating Index Ratio. The valve of the index ratio which corresponds to the values of specific resistance and permeability noted in the preceding paragraphs, to the given dimension a and to the selected frequency is termed the operating index ratio.

Current Values. Consider a charge of nonmagnetic material, and assume that the temperature of the charge is uniform throughout its mass. For that condition the value of the current in a core of the charge with radial thickness x measured from the vertical axis is given by the equation:

$$i_x = i_a\left(\frac{x}{a}\right)\left[\frac{\overline{\text{ber}'^2}\left(\frac{x}{p}\right)+\overline{\text{bei}'^2}\left(\frac{x}{p}\right)}{\overline{\text{ber}'^2}\,\Delta+\overline{\text{bei}'^2}\,\Delta}\right]^{1/2} \text{ amperes} \quad (4)$$

i_a = total current in the charge
a = radius of charge, centimeters

Volume V_p. The volume of the annular zone of the charge which is defined by the radial dimension p (the reference dimension), see Figure 2, per unit length is

$$V_p = \frac{\pi a^2}{\Delta}\left[2-\frac{1}{\Delta}\right] \text{ cubic centimeters} \quad (5)$$

Heat Development in Volume V_p. The value of the radial dimension x in equation 4 with reference to volume V_p is

$$x = (a-p) \quad (6)$$

Hence, in equation 4,

$$\frac{x}{a} = \left(\frac{a-p}{a}\right) = \left(1-\frac{1}{\Delta}\right) \quad (7)$$

and

$$\frac{x}{p} = \left(\frac{a-p}{p}\right) = (\Delta-1) \quad (8)$$

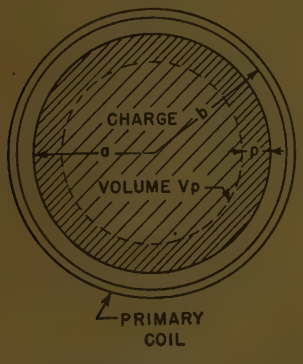


Figure 2. Relationship of the a, b, and p dimensions

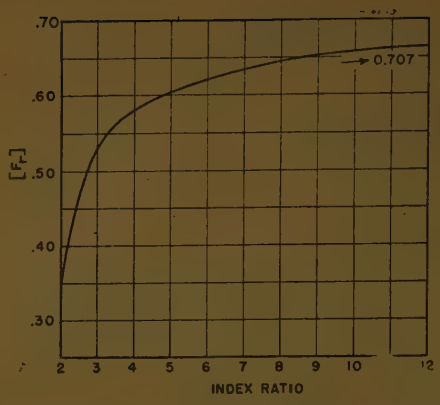


Figure 3. Values of the resistance function $[F_r]$ for solid cylindrical charges

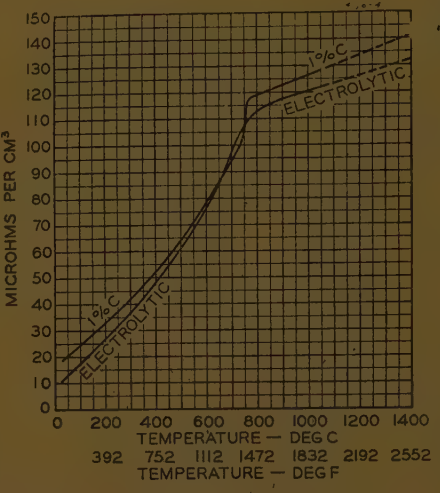


Figure 4. Resistivity—temperature relations of iron and one per cent carbon steel

If we substitute these values in equation 4,

$$i_{(a-p)} = i_a\left(1-\frac{1}{\Delta}\right)\times \left[\frac{\overline{\text{ber}'^2}(\Delta-1)+\overline{\text{bei}'^2}(\Delta-1)}{\overline{\text{ber}'^2}\,\Delta+\overline{\text{bei}'^2}\,\Delta}\right]^{1/2} \quad (4a)$$

For convenience write

$$i_{(a-p)} = g i_a \quad (4b)$$

The value of the current in the annular zone V_p is

$$i_p = (i_a - g i_a) = i_a(1-g) \text{ amperes} \quad (9)$$

The rate of heat development by eddy currents in volume V_p is thus

$$i_p^2 r_p = i_a^2 r_a - (g i_a)^2 r_a \text{ watts} \quad (10)$$

$$= i_a^2 r_a (1-g^2) \text{ watts} \quad (10a)$$

r_p = the resistance of the path of the current in annular zone V_p
r_a = the resistance of the path of the current in a core of the charge for any value of x up to and including $x=a$

The quantity $(1-g^2)$ in equation 10a is the fraction of the total heat development that is developed in volume V_p.

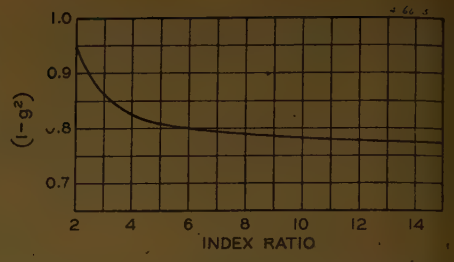


Figure 5. The fraction of the total heat development that is developed in volume V_p



Figure 6. Resistance characteristic of the secondary circuit

Values of this fraction for a range of values of the index ratio are given in Figure 5.

The graph of Figure 5 depicts only the comparative concentrations of heat in the charge for the assumed uniform-temperature condition. The result of the temperature gradient in the mass of the charge during a heat cycle is to increase the concentration of heat development. With this in mind, the concept given by Figure 5 is useful in its indication of the manner in which heat development by eddy currents occurs in a charge.

The Electrical Problem. The factors μ and ρ and the dimension a in the preceding equations are given values for each application of induction heating. The electrical problem is the selection of frequency and the corresponding choice of the relation between volts and amperes for:

(*a*). The required rate of heat development.

(*b*). The desired concentration of heat represented by the graph of Figure 5.

This problem can be contrasted with the problem of minimizing the eddy-current loss in the magnetic structures of electrical machines. Here with given and constant values of frequency, permeability, and resistivity, the factor of selection is the dimension a: for example, one-half the thickness of a sheet for the core of a transformer.

Resistance Characteristic. If resistivity is taken as a variable and the only variable, in equation 26 for the resistance of the secondary circuit of the transformer of Figure 1 the resistance characteristic of that circuit, Figure 6, is obtained.

Electrical Efficiency. The electrical losses of induction heating are the i^2r loss

in the primary coil, see Figure 1, and some stray field loss. If iron is included in the outer portion of the magnetic circuit, the consequent core loss is added to the previously noted losses.

When the stray field loss and the core loss, if any, are neglected, the efficiency of the transformation is

$$\eta = \frac{r_a}{r_b + r_a} = \frac{r_a}{R} \qquad (11)$$

r_a = the resistance of the secondary circuit
r_b = the resistance of the primary circuit

The resistance of the primary circuit, equation 23, is proportional to the square root of the frequency. The resistance of the secondary circuit, equation 26 is proportional to the product $[\sqrt{f}[F_r](Y)]$. If we consider frequency as a variable and start with a low value of the index ratio, the value of r_a increases faster than that of r_b and the efficiency of the transformation increase. This continues until with increasing frequency the product $[[F_r](Y)]$ becomes constant, see Figures 3 and 16. Thereafter, the efficiency is unaffected by increase of frequency. This characteristic is shown by curve *A* of Figure 7.

The one function of frequency in induction heating is to increase the resistance of the secondary circuit to obtain high efficiency of transformation. As the increase of efficiency by increase of frequency ends after a certain value of frequency is reached, there is no object in a higher frequency unless it may give a more desirable relation of volts and amperes or offer some economic advantage.

Power Factor. The power factor measured at the terminals of the primary coil of the transformer of Figure 1 is

$$\cos\varphi = \frac{R}{Z} \qquad (12)$$

R = the total resistance, equation 28
Z = the corresponding impedance, equation 30

The power-factor characteristic corresponding to the efficiency characteristic of Figure 7 is shown by curve *B*, Figure 7.

Base Frequency. The characteristic of Figure 6 and the characteristics, *A* and *B* of Figure 7 indicate that the frequency for a given assembly should be a value that gives an operating index ratio not less than 2.50. Hence, the frequency which corresponds to the index-ratio value 2.50 is designated as the base frequency of an assembly for induction heating.

The equation for the base frequency is obtained by substituting $\Delta = 2.50$ in equation 2, thus:

$$f_B = \frac{79.0 \times 10^6 \times \rho}{m^2} \qquad (13)$$

Example 1. $a = 3$ centimeters, $\rho = 114 \times 10^{-6}$ ohm-cubic centimeters, $\mu = 1.0$.

$$f_B = \frac{79 \times 114}{9} = 1{,}000 \text{ cycles}$$

Base Frequency and Index Ratio. Values of multiples of the base frequency and the corresponding values of the index ratio are given in Figure 8.

Example 2.

$f_B = 1{,}000$
$f_2 = 4{,}000$
$\Delta_2 = \sqrt{4} \times 2.50 = 5.0$

The graphs of Figure 9 show the base frequencies, $\Delta = 2.50$, and the frequencies which correspond to $\Delta = 3.50$ for a range of diameters of carbon steel at around 1,200 degrees centigrade.

Frequency and Current. The watts developed in a charge with the base frequency are

$$P_B = i_B^2 r_{aB} \qquad (14)$$

and with some other frequency, f_2,

$$P_2 = i_2^2 r_{a2} \qquad (15)$$

For the same rate of heat development in each case, that is, $P_2 = P_B$, the value of the current with frequency f_2 is

$$i_2 = i_B \left[\frac{r_{aB}}{r_{a2}}\right]^{1/2} \text{ amperes} \qquad (16)$$

The symbol r_a with appropriate added subscript denotes the resistance of the circuit in the charge, equation 26 of the appendix.

A graph of Equation 16 with reference to the index ratio is shown in Figure 10.

Frequency and Voltage. The voltage measured at the terminals of the primary coil with a given watts input to the charge with the base frequency is

$$e_B = i_B Z_B \text{ volts} \qquad (17)$$

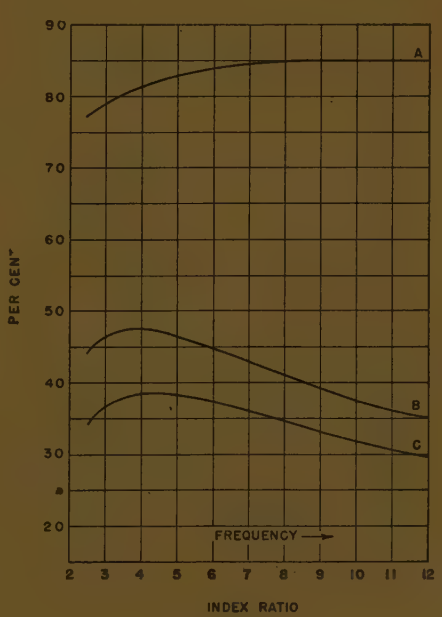


Figure 7

Efficiency–frequency characteristic, curve *A*
Power-factor–frequency characteristic, curve *B*
Curve *C*, the product of curves *A* and *B*

and with some other frequency, f_2,

$$e_2 = i_2 Z_2 \text{ volts} \qquad (18)$$

For the same rate of heat development in the charge in each case, that is, $P_2 = P_B$, as before the voltage with frequency f_2, is

$$e_2 = e_B \left[\frac{r_{aB}}{r_{a2}}\right]^{1/2} \frac{Z_2}{Z_B} \text{ volts} \qquad (19)$$

The symbol Z with appropriate subscript denotes the impedance of the circuit, equation 30 of the appendix.

Frequency and Kilovolt-Amperes. The product of equations 16 and 19 divided by 1,000 gives the relation,

$$\text{kva}_2 = \text{kva}_B \left[\frac{r_{aB}}{r_{a2}}\right] \frac{Z_2}{Z_B} \qquad (20)$$

Example 3. Numerical values for the circuit equations are necessary to

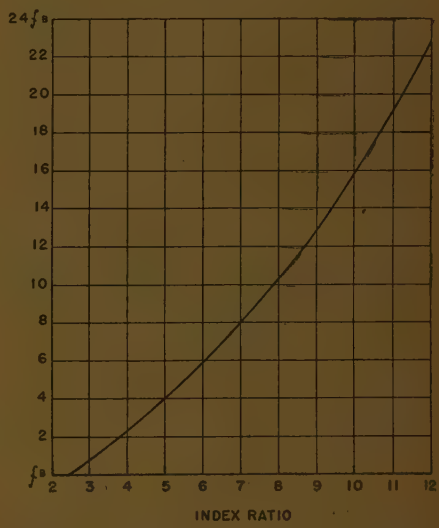


Figure 8. Values of multiples of the base frequency and corresponding values of the index ratio

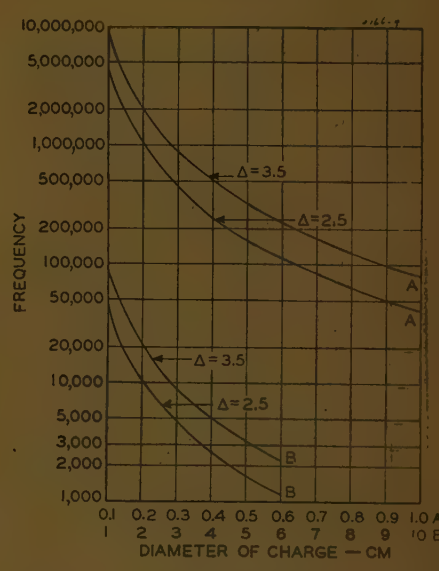


Figure 9. Values of frequencies for the index ratio values 2.50 and 3.50 for a range of diameters of carbon steel at 1,200 degrees centigrade

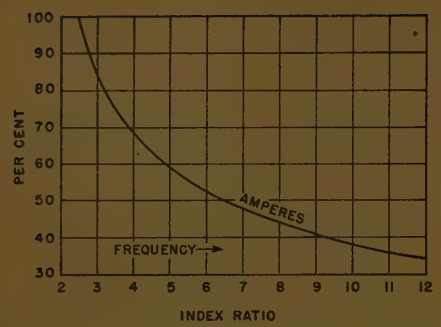


Figure 10. Values of current for a given watts input to a charge with frequencies above the base frequency

Values are percentages of the current with the base frequency

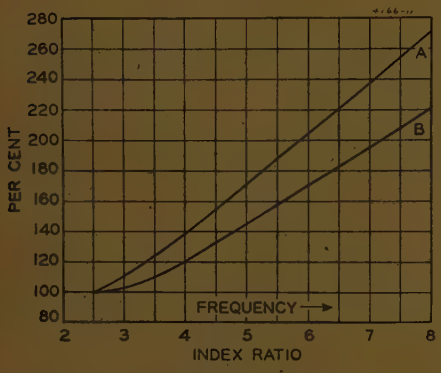


Figure 11. Voltages required for a given watts input to a charge with frequencies above the base frequency

Values are percentages of the voltage with the base frequency

Curve *A*, air-gap ratio, $\frac{b}{a}=1.20$

Curve *B*, air-gap ratio, $\frac{b}{a}=1.05$

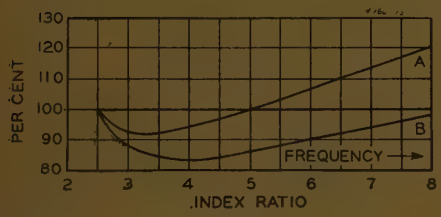


Figure 12. Kilovolt-amperes required for a given watts input to a charge with frequencies above the base frequency

Values are percentages of the kilovolt-amperes with the base frequency

Curve *A*, air-gap ratio 1.20

Curve *B*, air-gap ratio 1.05

illustrate the voltage relation of equation 19 and the kilovolt-ampere relation of equation 20. Any convenient set of values will serve. The data selected are

$a=3$ centimeters, $l=8$ centimeters, $\mu=1.0$, $\rho=102\times10^{-6}$ ohm-cubic centimeters

$N=6$ $\quad s=0.81$

Air-gap ratios, 1.20 and 1.05

Voltage Relation. The voltages required for a given watts input to a given charge with different frequencies, up to ten times the base frequency, with the two air-gap ratios, are given in Figure 11 as percentages of the voltages required for the given watts input with the base frequency.

Kilovolt-Ampere Relation. The values of kilovolt-amperes required for the given watts input to a charge under the conditions noted for the voltage relation of the preceding paragraph are given in Figure 12.

Example 3, continued,

$\frac{b}{a}=1.20$, curves *A*, Figures 11 and 12,

Base frequency, f_B..... 900 cycles

New frequency, f_2..... 8,100 cycles

New index ratio, Δ_2..... $\sqrt{9}\times2.60=7.50$

New current, i_2.....$0.46i_B$

New voltage, e_2.....$2.55e_B$

New kva$_2$.....1.17 kva$_B$

Frequency—Efficiency—Power Factor. The kilovolt-ampere relation of Figure 12 can be considered from the viewpoint of the product of efficiency and power factor, equations 11 and 12, respectively. The characteristic thus obtained is shown by curve *C* of Figure 7. This characteristic corresponds to curve *B* of Figure 12.

Optimum Frequency Range. The graphs of Figure 12 do not indicate a well-defined optimum frequency. There is an optimum range of frequency, and the smaller the air-gap ratio the wider is this range.

Service. The two divisions of induction-heating service are:

(*a*). Heating solids to temperatures below the melting points.

(*b*). Melting metals.

Heating Solids. The conditions of this service vary widely, both as regards the character of the charge and the purpose of the heat application. A general rule is that the desirable frequency is a frequency within the optimum frequency range.

Additional Factors. Two additional factors which affect the selection of frequency for heating a solid charge are:

(*a*). The watts per unit area of the peripheral surface of the charge.

(*b*). The length of the heat cycle.

The upper limit of "watts per unit area" is the value above which there is risk of melting the surface layer of the charge. This limit must be gauged by the ratio of the peripheral surface area of the charge and its volume. This ratio for a solid cylinder is

$$\frac{S_c}{V_c}=\frac{2\pi a}{\pi a^2}=\frac{2}{a} \qquad (21)$$

The limitations on the length of the heat cycle are:

(*a*). Grain growth.

(*b*). Surface oxidation.

The second limitation is a consequence of the "open-air" method of induction heating applied to solids.

A third factor not hereinbefore-mentioned is the depth of the hardened zone in the special application of heating steel for hardening.

The desirable depth of hardened zone in heating steel for hardening depends mainly on the type of steel and the service for which the part is intended.

These additional factors are matters of experience based on metallurgical considerations and cannot be embodied in general rules for practice. There is much useful information relating to these phases of induction heating in a paper by Vaughn, Farlow, and Meyer.[6]

It is often necessary to consider a range of diameters (thicknesses) of charges of a given material in the selection of frequency. The latitude of the optimum frequency range (if the air-gap ratio is taken into account) together with the degrees of freedom in the choice of "watts per unit area" and the length of heat cycle makes practicable the use of one frequency for a range of sizes of charges. Also, these conditions generally permit the use of a standard frequency in each case.

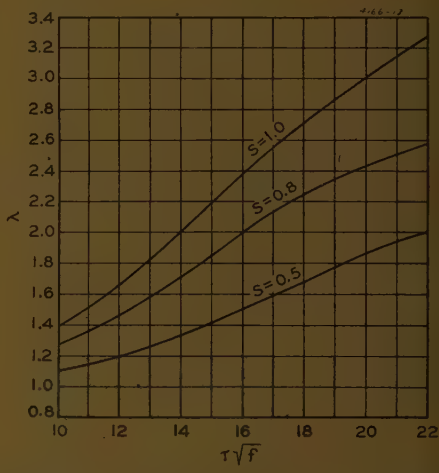


Figure 13. Values of λ for values of the product $\tau\sqrt{f}$

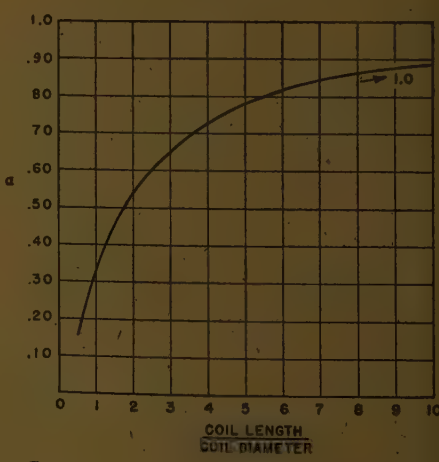


Figure 14. Values of α in equation 23

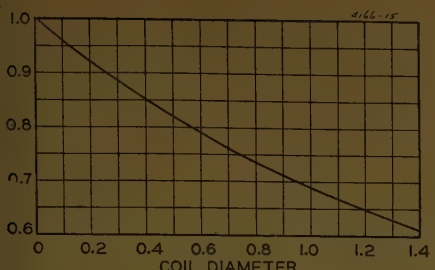


Figure 15. Values of K in equations 25 and 27

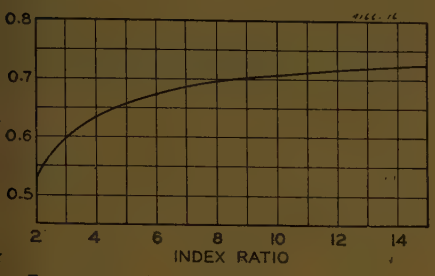


Figure 16. Values of Y in equation 26

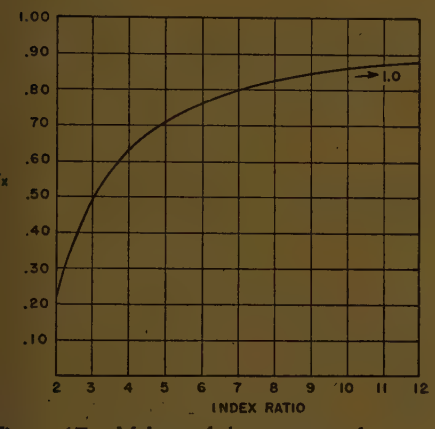


Figure 17. Values of the reactance function $[F_x]$ for solid cylindrical charges

Frequencies below the lower limit of the optimum frequency range are found in practice, particularly for type-3 charges. This practice gives qualitative consideration to the boosting effect of permeability as an aid in the initial rate of heat development. Also, frequencies above the upper limit of the optimum frequency range are in use. There is no technical objection to such frequencies where the practice is considered advantageous.

Melting Service. There are two special requirements which dictate the selection of frequency for metal-melting service. These are:

(*a*). The initial heating of a charge of scrap metal.

(*b*). The stirring of the molten metal by electrodynamic action.

Initially the index ratio is related to the nature of the charge and to the dimensions of the pieces of the charge. The frequency must be a value that gives a transformation efficiency high enough for effective initial heating, that is, to start the heat cycle.

After the charge has reached the liquid stage, the stirring of the metal by electrodynamic action is proportional to the square of the ampere turns. For a given watts input to the charge, the required ampere turns decrease with increase of frequency. The stirring of the liquid metal must be moderate in degree and the frequency must be suited to that requirement.

These two requirements and the economics of the service early led to the adoption in the United States of 960 cycles for steel-melting furnaces above 100-kva rating. The standard frequency for smaller furnaces is 3,000 cycles. Various higher frequencies are used for laboratory furnaces.

Appendix. Circuit Equations

The equations given herein are modifications of the theoretical equations of the circuits of the transformer of Figure 1. These equations were derived from experimental studies of circuits with frequencies up to 9,600 cycles and with charges with lengths not less than the diameters.

Primary Resistance. A single-layer coil with a water-cooled copper-conductor at 50 degrees centigrade is assumed.

The radial thickness of the conductor of this coil for the minimum a-c resistance of the coil is approximately

$$\tau = \frac{15}{\sqrt{f}} \text{ centimeters} \qquad (22)$$

The equation for the a-c resistance of the assumed coil is

$$r_b = [(r_b^0 - r_s)\alpha + r_s] \text{ ohm} \qquad (23)$$

$$r_b^0 = \frac{0.0018\sqrt{f}N^2b}{l\sqrt{s}\,10^3} \text{ ohm} \qquad (24)$$

f = frequency
N = number of turns
b = inside radius of coil, centimeters
l = length of coil, centimeters
s = space factor of the winding

$$r_s = \frac{r_b^0}{\lambda}$$

λ from Figure 13
τ = the radial thickness of the conductor of the coil
α from Figure 14

This method of calculating the a-c resistance of a coil is based on the work of Burch and Davis.[1]

If the radial thickness of the conductor is less than the dimension given by equation 22, the value of the a-c resistance of the coil approaches more and more closely the value given by equation 24.

Primary Reactance. The reactance of the primary coil is calculated by the equation,

$$x_b = \frac{8\pi^3 f N^2 b^2}{l\,10^9}(K_b) \text{ ohm} \qquad (25)$$

K_b = correction for the proportion of the coil, Table X of circular 74, National Bureau of Standards.[5] Some of these values are given in Figure 15.

Effective Radius. As a rule with the frequencies used for induction heating the use of the inner radius of the coil in equations 24 and 25 as the effective radius of the path of current in the primary conductor is of sufficient accuracy.

Primary Capacitance. Generally, the capacitance of the primary coil can be neglected. It may be desirable to take it into account with very high frequencies.

Secondary Resistance. The a-c resistance of the circuit formed in the charge is calculated by the equation,

$$r_a = \frac{2\pi\rho N^2}{l}\Delta[F_r\,](Y) \text{ ohm} \qquad (26)$$

ρ = the resistivity, ohm-cubic centimeters, of the material of the charge at its maximum temperature
N = number of turns in the primary coil
l = length of charge, centimeters
Δ = the index ratio corresponding to the previously noted value of resistivity
$[F_r]$ from Figure 3
Y = a correction factor from Figure 16

Secondary Reactance. The equation for the reactance of the secondary circuit is

$$x_a = \frac{8\pi^3 f N^2 a^2}{l\,10^9}[F_x](K_a')(Y)^{1/2} \text{ ohm}$$

$$[F_x] = \left(1 - \frac{2\theta}{\Delta}\right),$$

$$\theta = \left[\frac{\text{ber}\,\Delta\,\text{bei}'\,\Delta - \text{bei}\,\Delta\,\text{ber}'\,\Delta}{\text{ber}^2\,\Delta + \text{bei}^2\,\Delta}\right] \qquad (27)$$

A graph of values of $[F_x]$ for a range of values of the index ratio is given in Figure 17.

K_a' = a correction from Figure 15 on the basis of the ratio,

$$\frac{2a\left(1 - \frac{0.50}{\Delta}\right)}{l}$$

Combined circuit, primary and secondary:

Resistance, $R = (r_b + r_a)$ ohm (28)

Reactance, $X = (x_b - x_a)$ ohm (29)

Impedance, $Z = (R^2 + X^2)^{\frac{1}{2}}$ ohm (30)

References

1. Theory of Eddy-Current Heating (book), C. R. Burch, N. R. Davis. Ernest Benn, Ltd., London, England, 1928.

2. The Theory of Alternating Currents, volume I, second edition, A. Russel. Cambridge University Press, London, England, 1914.

3. The High-Frequency Resistance of Wires and Cables, G. W. O. Howe. *Journal* of the Institution of Electrical Engineers (London, England), volume 58, 1920, pages 152–70.

4. Calculations for Coreless Induction Furnaces, H. B. Dwight, M. M. Bagai. AIEE Transactions, volume 54, 1935, pages 312–15.

5. Radio Instruments and Measurements, circular 74, National Bureau of Standards, 1937.

6. Metallurgical Control of Induction Hardening, F. F. Vaughn, V. R. Farlow, E. R. Meyer. *Transactions* of American Society for Metals, volume 30, 1942, pages 516–40.

Gaseous-Discharge Lamps for Airplane Lighting Service

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NONMEMBER AIEE

FLUORESCENT lamps always have been considered desirable for general lighting within the airplane in order to reduce power consumption and utilize the long thin tubular-light sources that fit in so well in the interior of an airplane. Four-hundred-cycle operation has been found by Hays[1,2] to be excellent for fluorescent lamps, providing about 20 per cent greater efficiency than the 60-cycle alternating current for which these lamps were created originally and reducing the size and weight of the ballasts required. However, uncertainty as to lamp operation at low temperatures and particularly at the extremes of low temperature that might be encountered at tremendous altitudes has prevented the general use of these lamps in this field.

The fact that fluorescent lamps now are used for instrument lighting in military airplanes has indicated that the danger of starting and operating trouble is small, but, since these instrument lamps are enclosed within heat-conserving housings, it has been thought that the good performance they give may not be sufficient proof that larger fluorescent lamps will give equally good service in cabin lighting and other similar uses.

Similarly, while glow lamps now are used successfully in certain special applications in airplane lighting and for indicator service, very little has been recorded regarding their performance on the 400-cycle alternating current commonly used in airplanes. Consequently, a study was made of the effect of low-temperature and 400-cycle operation on these two types of lamps in order, if possible, to release them for general use in airplane service.

Fluorescent Lamps

The chief problem with the fluorescent lamp is to determine its performance throughout the normal anticipated range of ambient temperatures and also to carry this investigation down to the extremes of low temperature that might be encountered at high altitudes. In this low-temperature investigation the assumption was made that, in certain emergencies which might occur during the war, the interior of the airplane might reach the same temperature as the outside air. Such a condition probably would occur rarely if ever in civilian operation, but the information always would be of value in determining the practical as well as the theoretical range of usefulness for this type of lamp.

In this investigation standard 15-watt *T*-8 bulb lamps were tested in three representative types of installations. One consisted of the bare lamp exposed to natural flow of the surrounding air. The second consisted of the lamp with a fairly close-fitting reflector designed to conserve some of the heat generated by the lamp. The third consisted of a lamp surrounded by a glass tube sealed at each end so as to minimize the heat loss from the lamp when operated at low temperatures. These lamps were operated at both 400 and 60 cycles for comparison.

The lamps were placed in the cold box and brought to full equilibrium at various temperature points. Where small temperature changes caused large changes in lamp performance the time required to reach a steady state ranged up to an hour. Readings were made of voltage, amperage, and relative light output. Starting characteristics were checked throughout the temperature range. All values reported represent the average of several lamp runs, so that the values indicate quite closely what would be obtained in service.

The lamps were operated at normal wattage. At 400 cycles this required operation at a current of 0.400 ampere at which the lamps generate approximately 120 per cent of normal light output. This higher than normal current overloads the electrodes somewhat. Operation at normal amperes corresponding to about 13 watts and normal lumens may be found preferable for civilian use after the war.

Light Output

The tests revealed the important fact that this 15-watt *T*-8 lamp will stay lighted down to the extremely low temperatures encountered at high altitudes on either 60 or 400-cycle current. At the higher frequency they show a slightly better maintenance of light output at low temperatures and a larger factor of safety in operation.

The use of a glass cover plate over a flush built-in fixture is similar in effect to the glass enclosing tube used in the tests. Such an arrangement will result in reasonably well-maintained light output from fluorescent lamps operated for prolonged periods of time at ambient temperatures well below zero degrees Fahrenheit. Also, because it takes considerable time to cool the enclosed lamp, light will be maintained quite well for periods of a few minutes at the minimum temperatures used in the tests. It will maintain a level of illumination suitable for emergency use for prolonged periods of operation at temperatures below −50 degrees Fahrenheit.

Exposed lamps always will suffer a greater light loss at low ambient temperatures, depending of course on the degree of movement of the cold air at the surface of the lamp itself. A close-fitting reflector improves the conditions somewhat as shown by the curves. In general, such an arrangement of the reflector to conserve heat is well worth while and is recommended lighting practice to reduce glare.

Lamp Operation

Low-Temperature Starting. Fluorescent lamps are designed to be started by means of a starting switch of some sort. This may be a simple manual switch or an automatic device such as the standard glow switch or the thermal switch, which

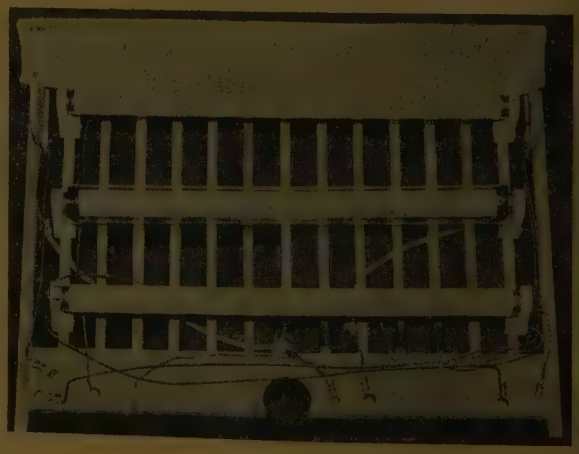


Figure 1. Test lamps were mounted on open wooden rack to test effect of cold surrounding air on lamp performance

Front lamp bare; middle lamp protected by glass-tube jacket; rear lamp operated beneath close-fitting reflector, reflector turned back to show mounting

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The author acknowledges experimental work done by V. H. Laughner, K. F. Molton, and A. J. Dussault.



Figure 2. Lamp rack installed in cold box

Door opened to show test setup. Equilibrium reached for each reading, requiring in some cases a period of two hours.



Figure 3. All lamp characteristics measured by meters outside cold box

Photoelectric cell and thermocouple inside chamber measured relative light output and ambient temperature at the lamp

is frequently used under adverse starting conditions such as at low temperatures. Various standard methods and modified schemes of starting were tested to determine starting reliability over the test range of temperature.

Lamp starting is critically affected by line surges, by capacitative effect of metal such as reflectors located adjacent to the lamp bulb, and by static effects in the air particularly as influenced by humidity. Not all possible combinations of circumstances were explored, and so this report on starting is merely a record of experiments run. It is probable, however, that in ordinary operating conditions the results will be about the same as found in these tests.

Starting of fluorescent lamps with the standard glow switch on 118-volt 60-cycle lines generally becomes somewhat undependable below 40 degrees Fahrenheit. Four-hundred-cycle operation is somewhat more favorable for starting, but good practice is to recommend glow-switch starters for temperature ranges down to 50 degrees Fahrenheit. The circuit is generally familiar and is shown in Figure 4. Below this point other starters should be used on both 60 and 400 cycles.

On 60-cycle circuits low-temperature conditions require the use of a thermal starter. This greatly improves starting and eliminates practically all danger of recycling of the starter. With such starters on 60 cycles standard 15-watt lamps and equipment are dependable down to zero degrees Fahrenheit. On 400 cycles, however, the thermal starter alone is not appreciably more effective than the glow switch, so other methods of starting are required for dependable starting at low temperatures.

For low-temperature operating conditions on 400 cycles a small series capacitor provides dependable starting with either the manual or the automatic thermal starter. Such a device is shown in the diagram in Figure 5. For the 15-watt *T*-8 lamp tested, a capacitor of one microfarad starts the lamp readily down to about −10 degrees Fahrenheit. A capacitor of $1^1/_2$ microfarad starts it down below −30 degrees Fahrenheit.

The use of the series capacitor for starting is highly effective for 400-cycle operation, but if the lamps are turned on and off frequently it tends to shorten lamp life and so is recommended only if assurance of dependable lamp starting at low temperatures is more important than long lamp life. Normal life on standard circuits at normal amperes (0.30 *A*) is 2,500 hours with the lamp turned on and off once for each three hours of burning. Using the $1^1/_2$-microfarad capacitor described previously and operating the lamps at 0.40 ampere, it is probable that lamp life will be reduced to an average of approximately 1,000 burning hours. Where a longer lamp life is desired the lamps may be operated at lower wattage.

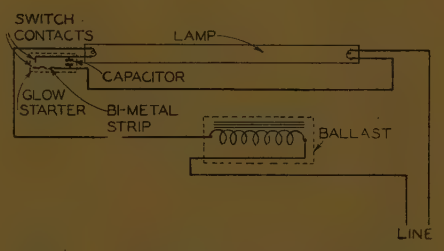


Figure 4. Conventional glow switch and choke circuit for fluorescent lamps suitable for 60 or 400 cycles

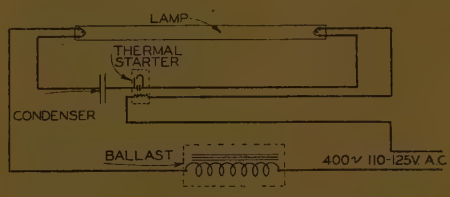
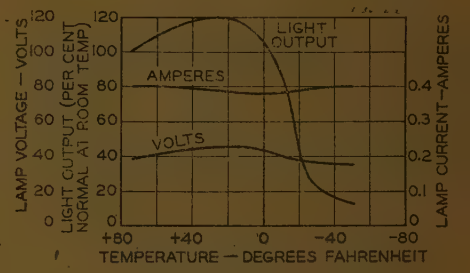


Figure 5. Special 400-cycle circuit to provide dependable starting of fluorescent lamps at low temperatures

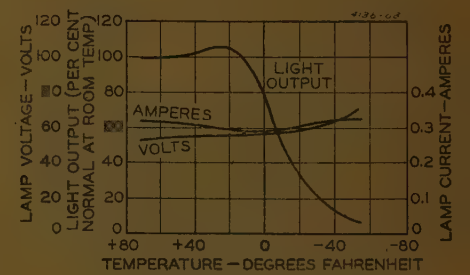
Steadiness and Recycling of Starters

No evidence of unsteady operation was noted during these tests. The light meter cell was inside the chamber and showed no minor fluctuations of light output. The voltmeter and ammeter also gave good indication of steady operation throughout the entire temperature range. In addition the door often was opened for a moment to observe conditions, and in no instance did unsteady operation show up.

Recycling of the standard glow-switch starter at low temperatures results when the cold raises the lamp arc voltage to a value equal to or above the breakdown voltage of the gas (neon or argon) in the glow switch itself. This causes the lamp to flash alternately on and off. The 15-watt *T*-8 lamp has a normal 60-cycle



A. 400 cycles, 118 volts

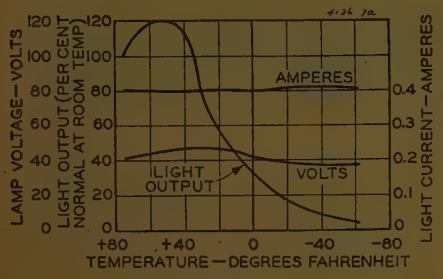


B. 60 cycles, 118 volts

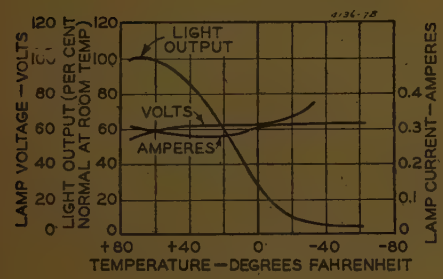
Figure 6. An enclosed fluorescent lamp maintains its light output at low temperatures, particularly on 400-cycle circuits

Tests run with clear glass cylinder around lamp as shown in Figure 1. Still air throughout all tests

voltage of 56 volts, while at 400 cycles the voltage is only 43 volts. The minimum breakdown voltage of the neon filling gas with the electrodes used in the standard *FS*-2 starter switches is 80 volts at 60 cycles and somewhat higher at 400 cycles. These breakdown volts are practically unchanged at any ambient temperatures that may be encountered. This provides a wide factor of safety with glow-switch starters in 400-cycle operation.



A. 400 cycles, 118 volts



B. 60 cycles, 118 volts

Figure 7. Close-fitting hood-type reflector above lamp tends to conserve lamp heat and maintain level of illumination somewhat better at low temperatures than with bare lamp

The curves in Figures 6A through 8A show that in no case on 400 cycles did the lamp voltage approach the critical value of 80 volts. Actually none of the lamps tested at either 60 or 400 cycles caused recycling of the glow-switch starter even at the lowest temperature reached. It is evident from the voltages obtained that there will be no danger with this size lamp of recycling in service, and it may be that on 400 cycles larger lamps, such as the 20-watt *T*-12 size can be operated without recycling at lower temperatures than on 60-cycle circuits.

Glow Lamps

Glow lamps are particularly well suited to 118-volt a-c airplane indicator service, because they consume very little power and are extremely rugged even in the small sizes generally used for indicator lights. Consequently, investigations were made to determine the effect of 400-cycle power on lamp operation and also the effect of low ambient temperatures on the lamp performance. Table I shows the starting volts above which dependable starting will be obtained for direct current, 60-cycle, and 400-cycle alternating current for several types of glow lamps suggested for airplane indicator service.

These values reported in Table I were obtained from several samples of each of the lamp types shown. Because of the inherent variation of the glow lamp, many more samplings and readings at various points throughout life must be made before the survey can be considered complete. However, the values given here are indicative of performance to be expected in service.

Table I

Lamp Type	Catalog No.	Approximate Starting Volts D-C	60 Cycles	400 Cycles
1/4 watt, *T*-4 1/2, candelabra, screw, neon	*NE*-45	90	65	76
1/2 watt, *G*-10, medium, screw, neon	*NE*-27	—	105	135
1 watt, *G*-10, medium, screw, neon	*NE*-30	85	60	69
2 watts, *S*-14, medium, screw, neon	*NE*-34	85	60	73

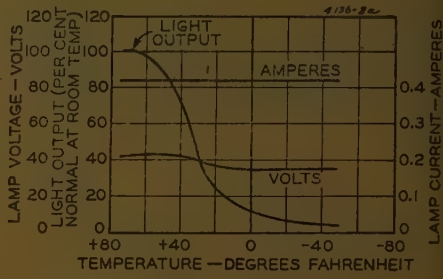
The starting volts shown in Table I are those values above which all new glow lamps should start. Individuals frequently start at lower voltages. On direct current only one electrode (negative) glows, producing approximately the same light output as the combined electrodes on alternating current. On 400-cycle circuits the output is practically identical to that generated at 60 cycles.

The effect of low ambient temperature on glow lamps was investigated on 400-cycle power. The experiments were run through a range of ambient temperatures from 70 degrees Fahrenheit down to —60 degrees Fahrenheit. Throughout this range the change in the light output and the operating characteristics of these glow lamps was negligible, even though the lamps were not enclosed but were exposed to the air.

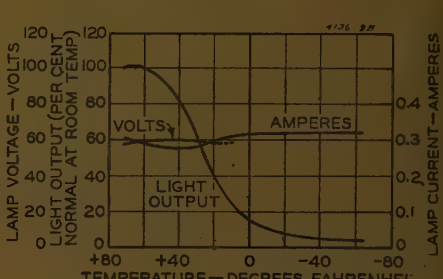
Conclusions

Fluorescent Lamps

It is evident from the experiments run that satisfactory light output, high efficiency of light production, and dependable operation of fluorescent lamps are readily obtainable on 400-cycle airplane circuits. Ordinary equipment with special lightweight efficient 400-cycle chokes is adequate for ordinary ranges of temperature such as will be encountered in areas which people will occupy in comfort. Where provisions are to be made for emergencies or where military necessity involves operation at lower ambient temperatures, the lamp should be enclosed to maintain light output, and manual or thermal starter switches with a small series capacitor should be used to provide dependable starting.



A. 400 cycles, 118 volts



B. 60 cycles, 118 volts

Figure 8. Simple mounting of bare lamp is satisfactory for normal ambient temperatures, although shielding of light from view of passengers is desirable to eliminate glare

Lower lamp volts on 400 cycles shown in curves eliminates all danger of recycling of glow switch at extremely low temperature ranges

Glow Lamps

Glow lamps are practically unaffected by low ambient temperatures, and their performance is excellent at 400 cycles. These small rugged low-wattage long-lived light sources are recommended particularly as indicator lights on circuits of relatively high voltage such as on the 118-volt a-c lighting systems of new large-sized airplanes. In that application they will provide a maximum of dependability with a minimum of power consumption.

References

1. Characteristics of Low-Voltage Fluorescent Lamps at High Frequencies, R. F. Hays. *Illuminating Engineering*, May 1941.

2. High Frequency Adapts Fluorescent Lamps to Aircraft, R. F. Hays. Electrical Engineering, volume 60, November 1941, page 539.

Hot-Spot Temperatures in Dry-Type Transformer Windings

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L. C. WHITMAN
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Synopsis: With the increasing use of dry-type transformers, attention has been focused upon the fact that the difference between the average temperature rise by resistance and the hottest-spot temperature rise is much greater than in liquid-immersed transformers.

In this paper there are given the results of an extended series of thermal tests on a variety of dry-type transformer coils. Data are presented in regard to the effect of different heights of coil stacks, different configurations, and, in addition, some essential precautions in making temperature measurements are pointed out. Hot-spot temperatures were investigated in coils cooled by natural draft and by forced-air circulation at different velocities for both directed flow and random flow.

Sufficient data are given to determine the ratio between the average temperature rise of the windings and the hottest-spot temperature rise. Based upon this relationship, the need is shown for review and modification of the conventional hot-spot allowances for dry-type transformers.

IN the last few years there have been major advances in the manufacture and availability of class *B* insulating materials, together with improvements in the design of structures using these materials.

This has made possible a large increase in the upper limit of voltage and output ratings of dry-type transformers. A wider range of applications is now possible and correspondingly, an increasingly great volume of these units is being built. These transformers are no longer special cases but should be regarded as an important class of transformers meriting serious and careful attention to their special problems and limitations, as well as attention to their possible advantages.

Previous to these developments, dry-type transformers were cooled by the conduction of heat through solid insulation to the enclosing case or were for the most part sufficiently small so that adequate cooling was easily obtained. With the advent of higher voltages and larger output ratings, satisfactory cooling becomes a major problem and unless such transformers are conservatively designed, winding hot-spot temperatures will exceed established standards. Excessive hot-spot temperatures will, of course, hasten the deterioration of the insulation and shorten the life expectancy of the transformer.

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The authors acknowledge the technical advice relative to the measurement of temperatures received from V. M. Montsinger and A. U. Welch of the power transformer division of the General Electric Company, Pittsfield, Mass.

Present Hot-Spot-Temperature Standards

At the present time, American Standards for Transformers[1] specifies a 55-degree-centigrade average temperature rise by resistance and a 10-degree-centigrade hot-spot allowance for transformers with class *A* insulation. These same standards give a limit of 80 degrees centigrade rise by resistance of transformer windings with class *B* insulation but no limit is given for either hot-spot increment or for hot-spot temperature rise. Data in this paper will show that the hot-spot increment for average temperature rises of 55 degrees centigrade and 80 degrees centigrade for dry-type air-cooled transformers using class *A* and class *B* insulations respectively will greatly exceed 10 degrees centigrade for the commonly used arrangements of windings.

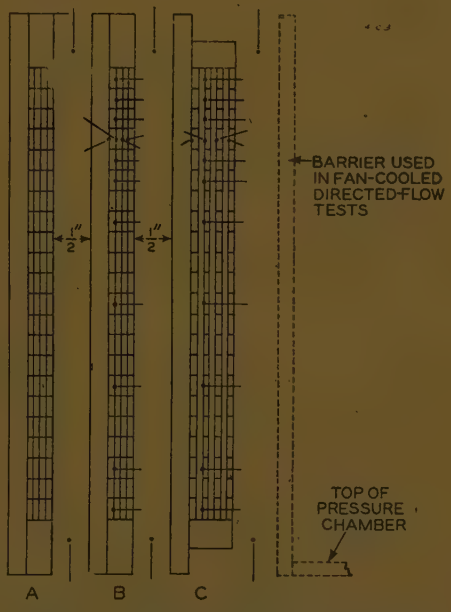


Figure 1. Typical arrangement of coils for tests, showing barrel coils with thermocouple locations

Temperature Measurement of Bodies in Air

It is necessary to take many precautions in the measurement of hot spots in windings cooled by air. First, it is highly desirable to insert measuring means into the winding itself. Measurement of the surface of the winding leads to inaccuracies since the radial gradient of these windings may be considerable, particularly when forced-draft cooling is used. Also, it is not clear whether it is the coil surface temperature or whether it is the temperature of the air film at the surface that is being measured. Coverings on the thermometer or thermocouple bulb reduce but do not eliminate this uncertainty. All measurements of winding hot spots as described in this paper were made by thermocouples buried in the winding and secured in place at the time of winding. Only turn insulation plus a very thin layer of asbestos separated the thermocouple from the copper of the winding in all cases. This totaled from 12 to 15 mils. When a thermocouple is in the maximum temperature level where there is no transfer of heat, the adjacent copper temperature is, of course, reached.



Figure 2. Disk coil showing thermocouple locations

All windings used in these tests were wound noninductively. This reduced the voltages required to approximately the *IR* drop of the conductors, permitted the use of alternating currents, and allowed thermocouple measurements to be made continuously while power was on without the necessity of taking cooling curves to correct back to shutdown for determining hot spots.

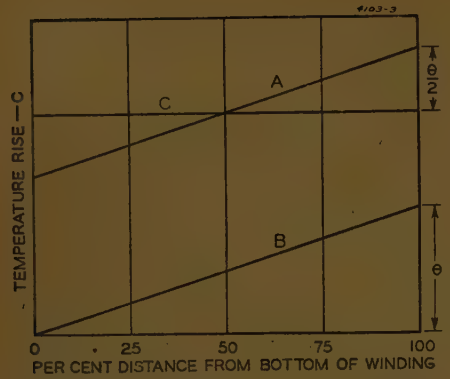


Figure 3. Previously proposed but incorrect axial winding and air gradients

A—Winding temperature rise by thermocouple
B—Air-duct temperature rise by thermocouple
C—Average winding temperature rise by resistance

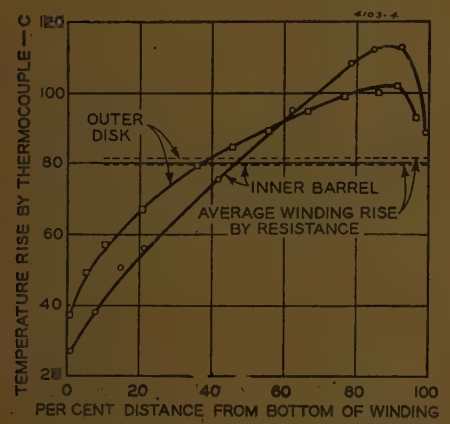


Figure 4. Axial winding gradients with natural-draft cooling for 30½-inch coils

To guard against the thermocouple wires conducting heat to or from the thermocouple bead and hence indicating an erroneous reading, these wires were buried in the coil in an isothermal region for a distance of at least six inches before being brought to the coil surface.

Average temperatures of the windings were determined by resistance measurements with cooling curves taken to correct temperature back to shutdown.

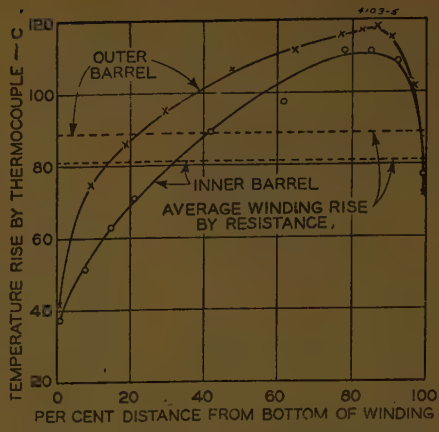


Figure 5. Axial winding gradients with fan cooling for 30½-inch barrel coils

In the case of air-temperature measurements, thermometer or thermocouple in an air stream will measure the temperature of the air only if it is protected from receiving or giving up heat due to radiation to or from other bodies. A silvered tube one inch long and three-eighths inch in diameter was used around the thermocouple bead in these tests with the bead held in a central position by means of cross threads. This allowed the air stream to contact the thermocouple but shielded it from receiving or losing much heat by radiation. In the configurations tested, an unshielded thermocouple indicated air temperatures several degrees in error.

Types of Windings

Two general types of coils are used in air-cooled dry-type transformers. The first type is coils having vertical ducts only which are hereafter referred to as "barrel" coils. They may be wound with or without layer insulation. Without layer insulation, the strands are generally connected in parallel and as such are referred to as "single-layer" coils even though they may be several strands high (see Figure 1, coils *A* and *B*). For coils of higher voltage, a barrel construction may still be used if layer insulation is introduced between layers. Such a coil is hereafter referred to as a "multilayer" coil (see Figure 1, coil *C*).

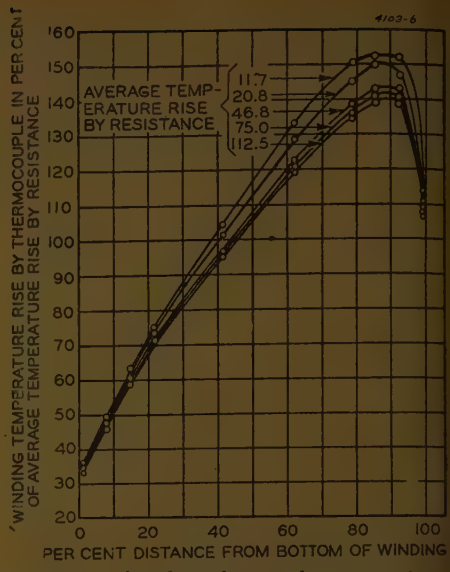


Figure 6. Axial winding gradients at various loads with natural-draft cooling for 30½-inch barrel coils

A second type of coil has an arrangement of horizontal ducts, as well as a vertical duct. These coils are hereafter referred to as "disk" coils (see Figure 2). Such coils are generally used as outside coils only as they usually have poor cooling efficiency if used as inside coils in dry-type transformers.

Types of Coils Tested

Barrel coils were tested in heights of 12½, 30½, and 60½ inches, actual winding lengths, and disk coils in 30½-inch stack height only. These coil heights are typical of dry-type transformers in ratings of 25 to 3,000 kva inclusive.

Ducts were in general one-half inch wide for vertical ducts and three-eighths inch for horizontal.

Arrangement of Coils

Coils were arranged as in Figure 1 for these tests. This arrangement has an in-

Table I. Heat Run Data on Typical Dry-Type Transformer Coils

(1) Winding Height, In.	(2) Air Velocity, Ft Per Min	(3) Average Rise by Resistance, C		(5) Hot-Spot Rise by Thermocouple, C		(7) Air Rise, C		(9) (10) Ratio: Hot-Spot Rise / Average Rise		(11) (12) Hot-Spot Increment, C		(13) (14) Ratio: Hot-Spot Increment / Air Rise	
		Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil	Inner Coil	Outer Coil
12½	†	72.9	70.3	95.0	83.4	53.1	26.0	1.30	1.19	22.1	13.1	0.415	0.50
30½	†	76.0	72.7	106.8	94.3	72.7	21.0	1.40	1.30	30.8	21.6	0.423	1.03
30½	†	74.5	74.3*	104.4	90.3*	77.3	57.3*	1.40	1.21*	29.9	16.0*	0.387	0.28*
60½	†	74.5	74.0	110.6	99.9	83.1	20.4	1.48	1.35	36.1	25.9	0.433	1.27
30½	**	82.6	84.4*	125.2	125.2*	59.8	56.7*	1.52	1.49*	42.6	40.8*	0.713	0.72*
30½	530	86.0	86.4	121.7	119.7	46.3	13.4	1.42	1.39	35.7	33.3	0.77	2.48
30½	2,015	81.2	89.3	111.3	118.3	28.2	22.1	1.37	1.33	30.1	29.0	1.07	1.31
60½	490	68.2	67.7	106.2	101.1	55.9	23.5	1.56	1.49	38.0	33.4	0.680	1.42
60½	1,510	80.4	81.9	120.8	117.9	58.1	20.4	1.50	1.44	40.4	36.0	0.695	1.76

† Natural draft. * Disk-type winding similar to Figure 2. ** External 12-inch fans. Velocity in inner ducts 450 feet per minute

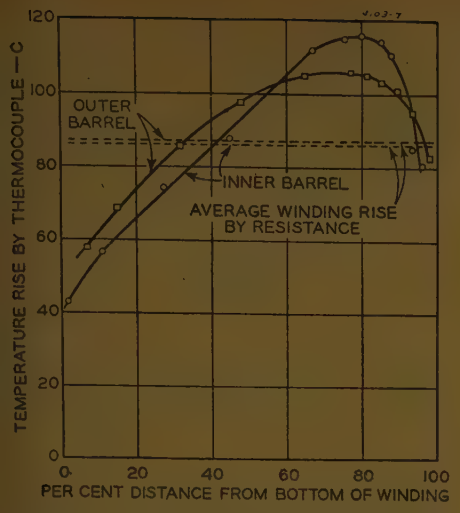


Figure 7. Axial winding gradients with natural-draft cooling for 12½-inch barrel coils

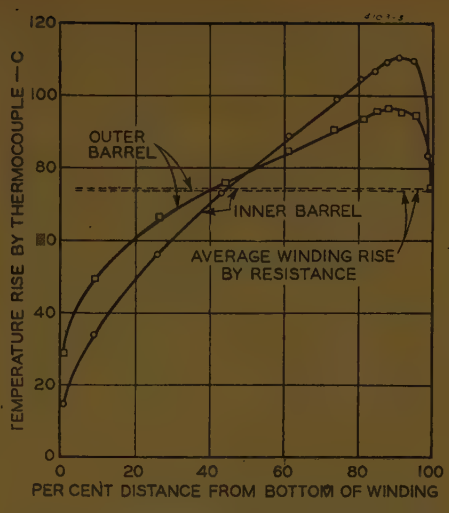


Figure 8. Axial winding gradients with natural-draft cooling for 60½-inch barrel coils

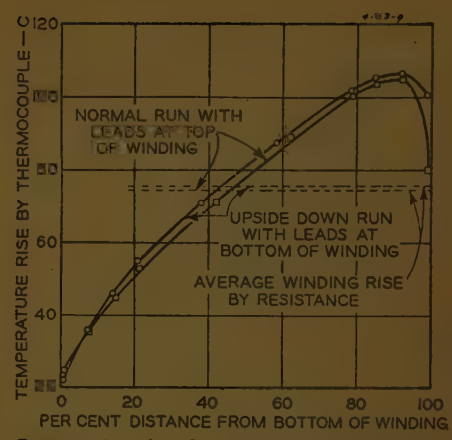


Figure 9. Axial winding gradients with natural-draft cooling showing effect of leads

ner coil *A* which provided the heat that would normally be supplied by a core or an adjacent winding. No core iron was used but a core and yokes of wood were built into each test setup which approximated the air restrictions which would exist in an actual transformer. Windings *A* and *B* were connected in series so that their average temperature rise was within about five per cent of each other. A tap was provided between windings *A* and *B* so that average temperature rise by resistance of each could be determined.

Since the coils were wound noninductively, the currents in windings *A–B* could be adjusted independently of that in winding *C*. In well-designed transformers, the temperature rises of all windings should be approximately equal; therefore, in these tests the currents in all windings were adjusted to give approximately this result.

Types of Cooling

In the smaller sizes, dry-type transformers are usually cooled by natural-draft air circulation. In the larger sizes, fan cooling of some form can be efficiently used. Fan cooling can be generally divided into two types, the so-called directed flow and random flow.

In the directed-flow type of cooling, a motor-driven fan builds up a static pressure in a compartment whose only outlets are through the cooling ducts of the coils and core. Hence, there is a definite and controllable pressure drop across these cooling ducts and consequently a definite and controllable velocity of air flow in all these ducts. If these cooling ducts are designed so as to have equal frictional entrance and exit losses, the air velocities in these ducts will be equal. In practice it may be desirable to design these ducts to have somewhat different air velocities so as to cool some parts of the coils more than others due to differences in loss per unit area of the surfaces. It is thus evident that this method of fan cooling permits accurate design and prediction of winding temperatures. In the test setup an additional cylinder was placed around the outer coil to direct the air flow past this coil and so obtain directed flow.

The random-flow type of cooling utilizes fans blowing air freely against the transformer core and coils. The air streams in this case are directed only by the location of the fans with relation to the windings. Considerable care and a trial-and-error method is necessary to give maximum efficiency to such an arrangement.

Axial Gradients in Windings

It might seem reasonable to assume that the axial gradient in these air-cooled coils would be a straight line whose maximum value at the top of the winding would be the average rise by resistance of the winding plus one half of the temperature rise of the air which flows past these windings. Such incorrectly assumed gradients and relations are shown in Figure 3.

Actual gradients were measured in these tests by a series of thermocouples embedded in the coils. Locations of thermocouples in the barrel and disk coils are shown in Figures 1 and 2. Gradient curves from actual data are shown in Figures 4 and 5 for both natural-draft and fan-cooled units. A marked feature of the vertical coil gradients is the turnover of the curves at the upper end. The hot spot was found to be at some point from 75 to 95 per cent of the winding height, being higher for taller coils. A series of curves taken at various loads is shown in Figure 6.

This turnover at the upper end is due to cooling of the top few turns caused partly by radiation and convection at the end of the coil, but primarily by conduction of heat away by the foundation cylinders and the coil leads. The effect of the leads is strikingly shown by inverting a coil so that leads are alternately at the top and the bottom. The change in the gradients in these two cases is shown in Figure 9.

Table I gives some of the test data on

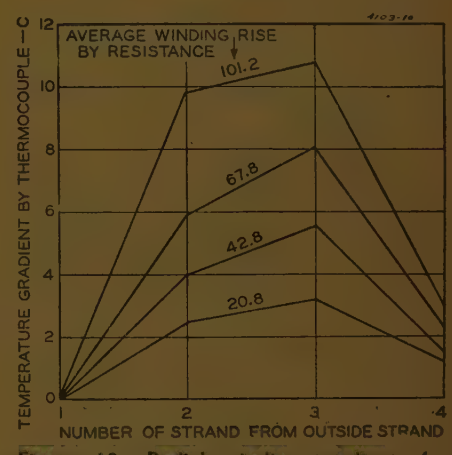


Figure 10. Radial winding gradients for 60½-inch four-strand inner barrel winding, fan-cooled

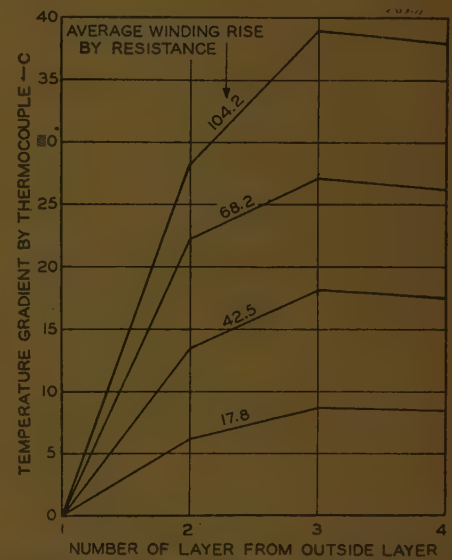


Figure 11. Radial winding gradients for 60½-inch four-layer outer barrel winding, fan-cooled

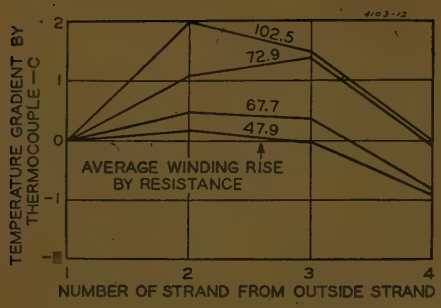


Figure 12. Radial winding gradients for 12½-inch four-strand inner barrel winding, natural-draft-cooled

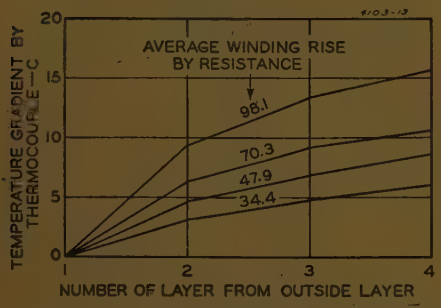


Figure 13. Radial winding gradients for 12½-inch four-layer outer barrel winding, natural-draft-cooled

hot spots, together with air rises for some typical natural-draft and fan-cooled designs.

For natural-draft inner-barrel coils, the hot spot can be closely approximated by adding 40 per cent of the temperature rise of the air flowing past these coils to the average temperature rise by resistance of the coil. This compares with the 50 per cent factor that would be expected if the vertical gradients were straight lines. This approach to hot-spot determination is not correct, however, in the case of outer windings where this factor varies considerably, nor in the case of fan-cooled windings where the factor increases to the order of 100 per cent or more. The failure of this rule for outside windings cooled by natural draft occurs because all the cooling air is not in a duct and hence not under control, cooler air continually joining the upward flow of the air currents on the outside face of the coil and so decreasing the measured air temperature.

Radial Gradients in Windings

Radial gradients in coils are a function of turn and layer insulation, loss per unit area, velocity of air by the coil, and number of strands high radially. It is obvious that fan cooling increases the velocity of air by the coils by a factor of several times that due to natural draft and so produces a larger temperature gradient from the inside to the outside of a coil. Also, due to the over-all cooling effect, the loss per unit area of such coils can be raised by a considerable factor without exceeding guaranteed rises. This totals to a considerable radial gradient in the case of fan-cooled units. Typical radial gradients for fan-cooled units are shown in Figures 10 and 11. In all cases the radial gradients shown are taken at or near the height that the maximum axial temperature occurs.

The radial gradients of inner coils are considerably greater in the case of fan cooling as compared with natural-draft cooling. The contrast in the case of outer coils is somewhat less since the radial gradient is already quite large due to loss of heat by the coil through radiation and also its contact with cool ambient air.

The radial gradients of inner coils cooled by natural draft are small and the effect of axial height on the radial gradient is correspondingly small. The effect of axial height on the radial gradient of outer coils is considerable, however, being more than two to one for the range of heights investigated. The effect of radiation is clearly shown when comparing the radial gradients of outer and inner coils, as for example Figures 10 and 11.

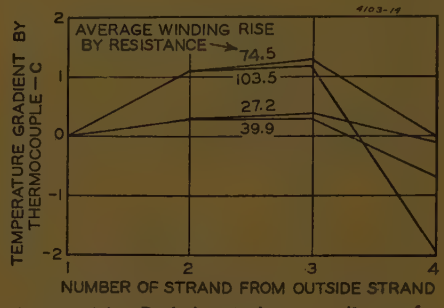


Figure 14. Radial winding gradients for 60½-inch four-strand inner barrel winding, natural-draft-cooled

Hot-Spot Allowance as a Function of Rise by Resistance

For any given coil configuration, the data for natural-draft cooling and fan cooling can be plotted to give a straight line using average winding rise by resistance as abscissa and hot-spot rise minus average winding rise by resistance as ordinate. This latter may be termed the "hot-spot increment"; hence, from geometry,

$$Y = mX$$

where

Y = hot-spot increment
X = average winding rise by resistance

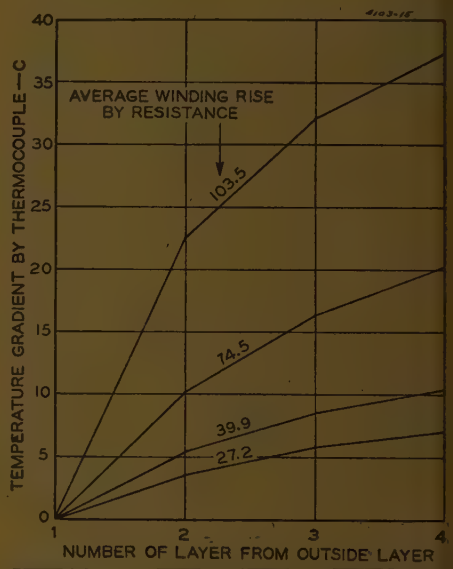


Figure 15. Radial winding gradients for 60½-inch four-layer outer barrel winding, natural-draft-cooled

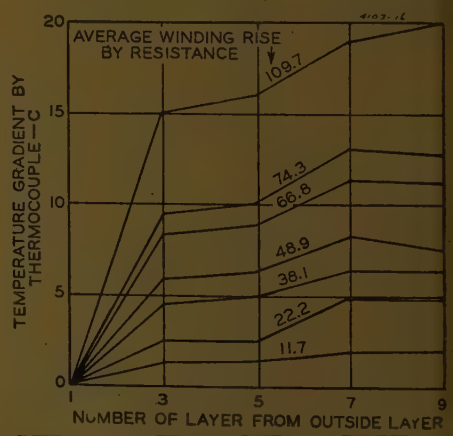


Figure 16. Radial winding gradients for 30½-inch ten-layer outer disk-type winding, natural-draft-cooled

Table II. Hot-Spot Increments in Degrees Centigrade for Dry-Type Transformers

Natural-Draft-Cooled

Average Winding Temperature Rise by Resistance, C	Outer Barrel Coils; Coil Height, Inches			Inner Barrel Coils; Coil Height, Inches			Outer Disk Coils; 30½ In. Height, 1 In. Build
	12½	30½	60½	12½	30½	60½	
45	9.5	12.5	18.5	16.5	19.5	21.5	10.5
50	10.5	14.0	20.5	18.0	21.5	24.0	11.5
55	11.5	15.5	22.5	19.0	23.0	26.5	12.5
60	12.5	17.0	24.5	20.5	25.0	29.0	13.5
65	13.5	18.5	26.5	21.5	27.0	31.5	14.5
70	14.5	20.0	29.0	22.5	29.0	34.0	15.5
75	15.5	21.5	31.0	24.0	30.5	36.5	16.5
80	16.5	23.0	33.0	25.0	32.5	39.0	17.5
90	19.0	25.5	37.0	27.5	36.0	43.5	19.5
100	21.0	28.5	41.0	30.0	40.0	48.0	21.5

Table III. Hot-Spot Increments in Degrees Centigrade for Dry-Type Transformers

Fan-Cooled

Average Winding Temperature Rise by Resistance, C	Outer Barrel Coils; Coil Height, Inches				Inner Barrel Coils; Coil Height, Inches				Outer Disk Coils; $30^1/_2$ In. Height, 1 In. Build
	$30^1/_2$		$60^1/_2$		$30^1/_2$		$60^1/_2$		
Air Velocity, Ft Per Min	530	2,015	490	1,515	530	2,015	490	1,515	450
45	16.5	15.5	22.0	21.0	20.0	18.5	25.5	23.0	23.0
50	18.5	17.0	24.5	23.0	22.0	20.0	28.5	25.0	25.5
55	20.5	18.5	27.0	25.5	23.5	22.0	31.5	27.5	28.0
60	22.5	20.0	30.0	27.5	25.5	24.0	34.5	30.0	30.5
65	24.5	21.5	32.5	29.5	27.5	25.5	37.5	32.5	33.0
70	26.5	23.0	35.0	32.0	29.5	27.5	40.5	35.0	35.5
75	28.5	24.5	37.5	34.0	31.5	29.0	43.5	37.5	37.5
80	30.5	26.0	40.0	36.0	33.5	31.0	46.5	40.0	40.5
90	34.5	29.0	45.5	40.5	37.0	34.5	52.5	44.5	45.5
100	38.5	32.0	50.5	45.0	41.0	38.0	58.5	49.5	50.0

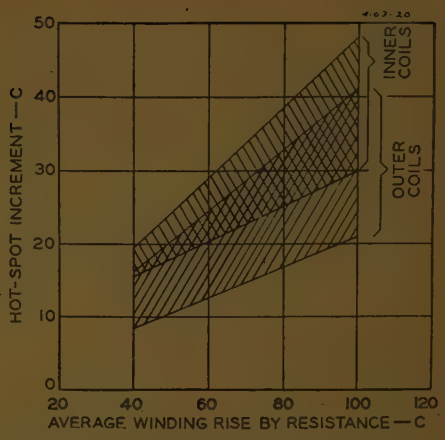


Figure 20. Winding hot-spot increments for natural-draft cooling

m = 0.21 to 0.58 depending upon coil configuration (slope of the line)

This same relation may also be expressed as a ratio of hot-spot temperature rise by thermocouple to average rise by resistance as shown in columns 9 and 10 of Table I.

The coil height is a considerable factor in the hot-spot increment. This is true for both inner and outer coils.

These data can be further correlated into bands by noticing that outer coils cooled by natural draft fall into a lower band than inner coils cooled by natural draft (see Figure 20). While these bands are quite wide, it should be noted that test data cover coils of widely differing heights and layer insulation, as well as different types. Disk coils of rather small build and of medium height (approximately one-inch build and $30^1/_2$-inch stack height) were used and this undoubtedly accounts for their being near the lower side of the band (lower curve in Figure 19). It is to be noted that there is about a five-degree-centigrade difference in the hot-spot increment between inner and outer coils in the ordinary range of temperature rises. This is explained by the loss of heat throughout the height of the coil by the radiation component.

Fan-cooled coils were tested in heights of $30^1/_2$ inches and $60^1/_2$ inches only, since at the present time it is believed to be economically feasible to build only the larger kilovolt-ampere sizes of dry-type transformers fan cooled. It is to be expected that there is only a small difference in the hot spots of inner and outer coils cooled by forced draft since in both cases the cooling is mainly by convection, and the cooling by radiation of the outer coil is a small percentage of the total. The data substantiate this idea. Since the radial gradients for fan-cooled coils are higher than for natural draft, the hot spots are slightly more, as might be expected.

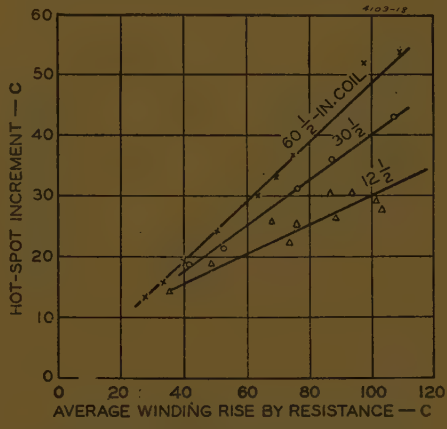


Figure 18. Hot-spot increments for four-strand single-layer inner barrel coils, natural-draft-cooled

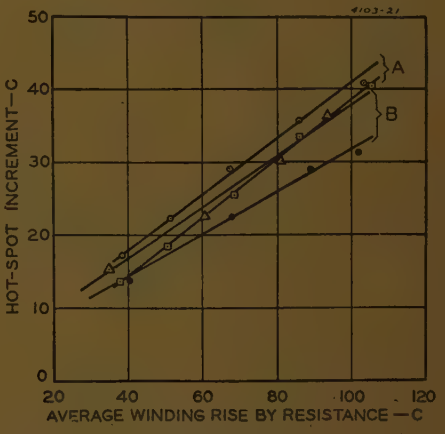


Figure 21. Hot-spot increments for $30^1/_2$-inch inner and outer barrel coils, fan-cooled

A—Cooling-air velocity 530 feet per minute
B—Cooling-air velocity 2,015 feet per minute
⊙—Inner coil △—Inner coil
⊡—Outer coil ●—Outer coil

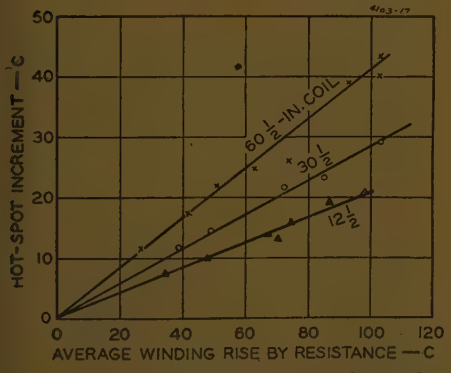


Figure 17. Hot-spot increments for multi-layer outer barrel coils, natural-draft-cooled

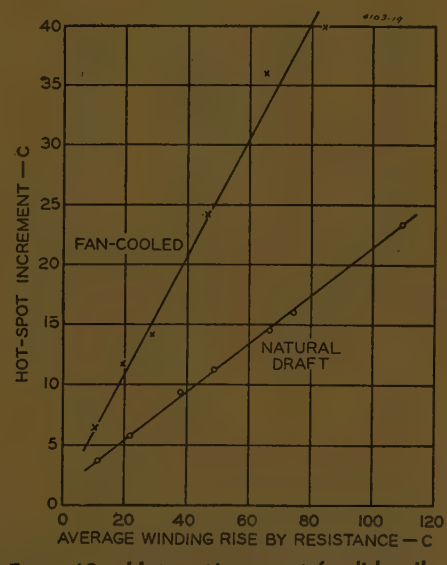


Figure 19. Hot-spot increments for disk coils, natural-draft and fan-cooled

There is some effect of change of air velocity, the lower velocities giving a higher hot-spot. Due to the fact that higher velocities can be somewhat more easily obtained in directed-flow design than in the random-flow design, there is some advantage in the use of this type of construction as a means of control for hot-spot rise.

A correlation of natural-draft and fan-cooled hot-spot increments for $30^1/_2$- and $60^1/_2$-inch coils, as shown in Figure 23, indicates that the fan-cooled designs may have slightly higher hot spots, especially if low-velocity cooling is used.

Air-Duct Rise as a Function of Winding Rise by Resistance

The air-duct temperature rise is a straight-line function of rise by resistance. It is interesting to note that for medium-

height coils operated natural draft, the temperature rise in one-half-inch-wide air ducts is approximately equal to the average temperature rise of the coil. The height of coil affects this air-duct temperature rise, the temperature rise being higher for longer coils. Duct width is also a factor, the change being roughly inversely proportional to duct width within a limit of usual dimensions.

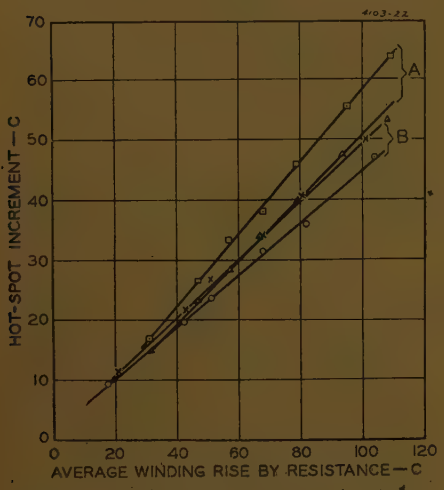


Figure 22. Hot-spot increments for 60½-inch inner and outer barrel coils, fan-cooled

A—Cooling-air velocity 490 feet per minute
B—Cooling-air velocity 1,515 feet per minute

⊡—Inner coil ×—Inner coil
△—Outer coil ⊙—Outer coil

Effect of Enclosing Case

The data in this paper were taken mainly with the windings in the open air. This approximates the conditions when expanded metal cases are used. In order to check the effect of a metal-clad case, such as might be provided by a sheet-metal case or the modern-styled louvered case, some runs were made with an enclosing metal shell with openings at top and bottom. The hot-spot increment was not affected within the limits of measurement by the enclosing metal shell.

Discrepancies Between Existing Standards and Test Data

For 55 degrees centigrade rise (class *A* insulation), Tables II and III show hot-spot increments for dry-type transformers ranging from about 11 degrees centigrade for very short natural-draft coils to about 31 degrees centigrade for tall fan-cooled coils. This is contrasted with the flat 10-degree centigrade hot-spot temperature allowance in American Standards for Transformers[1] with class *A* insulation.

For 80 degrees centigrade rise (class *B* insulation), these same tables show hot-spot increments ranging from about 16 degrees centigrade to about 46 degrees centigrade, depending upon coil length and method of cooling. There is no hot-spot temperature allowance given in American Standards for class *B* insulation. The only bench mark for "hottest-spot temperature" for class *B* insulation is given in introduction to AIEE Standard 1 as 130 degrees centigrade. This, in conjunction with the previously used 40-degree-centigrade ambient, gives a 10-degree-centigrade hot-spot temperature allowance. If an average ambient temperature of 30 degrees centigrade is used as given in American Transformer Standards,[1] this allowance becomes 20 degrees centigrade. In most cases, these allowances are considerably less than those shown by the test data in this paper.

It is thus evident that the hottest-spot allowances in the Standards are not consistent with those actually existing in dry-type transformers.

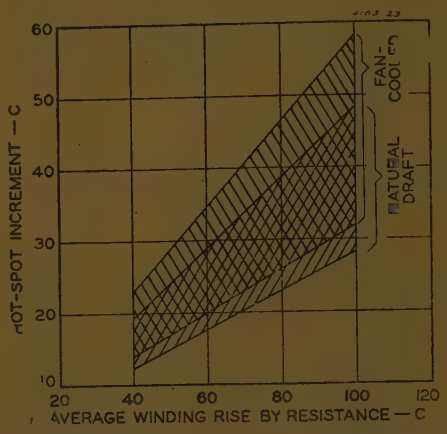


Figure 23. Winding hot-spot increments for natural-draftand fan cooling for 30½-inch and 60½-inch coils

Conclusions

1. It has been established by tests on the wide variety of sample coils described in this paper that hot spots much greater than heretofore supposed exist in modern dry-type transformers of normal design dimensions.

2. There is no consistent relationship between the temperature rise of the air in the coil duct and the hot-spot temperature increment. In general, this increment is less than one half the air rise for natural-draft cooling and more than one half the air rise for forced-draft cooling.

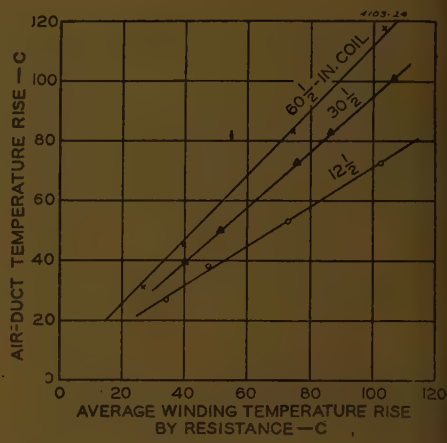


Figure 24. Air-duct temperature rises for barrel-type coils with natural-draft cooling

3. For a given dry-type transformer-coil design, the ratio between the hot-spot temperature rise and the average temperature rise by resistance remains constant when the average temperature rise is varied by changing the load.

4. This ratio increases with coil height.

This ratio is greater for inner coils than for the outside coil.

For a given coil design and air velocities normally used, this ratio is greater for forced-draft than for natural-draft cooling.

This ratio varies somewhat with the type of coil design.

5. No single hot-spot increment applies for all types and ratings of dry-type transformers because of the considerable number of variables affecting the hot spot.

6. The high hot-spot increments found in these tests show the need of review and modification of the conventional allowances for dry-type transformers.

References

1. American Standard for Transformers, Regulators, and Reactors C57.1, 1942.

2. AIEE Standard 1, 1940. Introduction.

Some Aspects of the Application of Induction Motors to Aircraft

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ASSOCIATE AIEE

Synopsis: Fundamental characteristics of the three-phase induction motor are reviewed with respect to their possible application to aircraft. A brief review of present motor specifications is presented, and test data are given on the operation of an induction motor with one or two lines shot away. Common types of motor loading are discussed with reference to the suitability of induction-motor drive for each. Curves are presented to illustrate the characteristics of motors and various types of loading.

USE of electric power in aircraft has grown with each succeeding year. Aircraft have become larger, and with each succeeding increase in size new and greater demands have been made for electric power. To date these demands have been met magnificently by 12-volt and 24-volt d-c systems, and there seems every reason to suppose that these systems will be in use for a long time to come. However, there is a transmission-weight penalty for a 24-volt system on large airplanes, particularly when the total load is high. Therefore, with an eye to the very large planes of the future, the aircraft industry has been fostering the development of two new electric systems, namely, a 115-volt d-c system, and a 208-volt three-phase 400-cycle system. Either of these two proposed systems will reduce greatly the weight of the electric wiring, and each system has its own special merits.

Motors constitute the principal electric load on military aircraft. A knowledge of motor characteristics is essential to a proper application of these motors to aircraft accessories and to the selection of the electric system itself. This paper presents the fundamental characteristics of the three-phase induction motor and some of the problems involved in applying them to aircraft use. The simplicity of construction, reliability, and lack of commutators and brushes recommends this motor very highly. However, there are certain disadvantages which must be considered, such as, lack of a high ratio of starting to running torque and lack of speed control. In addition to these drawbacks the conditions encountered in applying any piece of apparatus to aircraft must be considered. These conditions are much more severe than those encountered in industrial practice. This point can be understood easily when it is considered that an airplane may take off from a steaming jungle and within an hour be at an altitude of from 20,000 to 40,000 feet, where the temperatures are anywhere from −30 degrees centigrade to −70 degrees centigrade; and in a few hours it may fly far enough, so that when the plane lands the temperature would still be from −50 degrees centigrade to −70 degrees centigrade. Of course, this cycle easily could be reversed. In industrial practice it is thought necessary to rerate the motor with changes in temperature and changes in altitude, but an aircraft motor must operate at its rating under all of these conditions. In addition these motors must be light in weight. Compared with their commercial counterparts, they are mere pygmies. This means that each part of these motors is worked to its utmost with a very small safety factor. Therefore, in applying a motor to a given job, care must be taken that additional material is incorporated in the design in those places which must take the extra wear and tear demanded by the application.

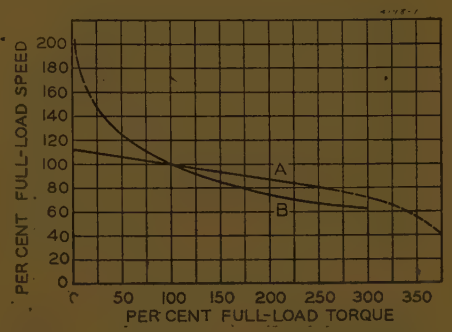


Figure 1. Typical d-c motor speed–torque curves

A. Shunt motor
B. Series motor

Performance Specifications

In order that the engineers who make the applications of the motors, and manufacturers who build the motors, may have a common language and understand each other properly, the United States Army Air Forces together with the manufacturers of motors have written a preliminary performance specification. They first picked a foundation frequency. This frequency is 400 cycles per second, which is a compromise between the 800 cycles per second that has been used for aircraft radio and the 240 cycles per second that was believed best for motors. Since this preliminary specification does tend to give a common language or picture of the motors, and since it does give a basis on which they can be rated, a review of the more common points will be found in Table I.

Speed-Torque Characteristics

For years the aircraft industry has been using small 12- and 24-volt d-c motors to perform the thousand and one odd jobs necessary to the successful flight of the modern airplane. Those who make the various applications of these motors to these jobs are familiar with the characteristics of them and will recognize easily the speed-torque curves given in Figure 1 as the characteristics which apply to the two types of d-c motors most used on aircraft namely, shunt and series motors. In order to apply an a-c motor successfully to these same jobs, the torque characteristics of the a-c motors must become familiar to those who are making the application.

Figure 2 gives typical torque curves for the two classifications of polyphase induction motors given in the table. These curves have been smoothed. Under ordinary conditions the curves for these individual motors are not smooth, but they are filled with small ragged variations caused by the distribution of flux in the motor and the winding distribution of the motor. Figure 3 shows one example of an actual curve of a motor with all the important variations. This curve was taken in the normal manner by taking readings at set points. Therefore, it does not show all of the minute individual

Table I. Motor Performance Specification

	Continuous-Duty Motors	Intermittent-Duty Motors
Frequency	400	400
Line voltage	200	200
Duty cycle	Continuous	1 minute in 20
Allowable frequency variation	±5%	±5%
Allowable voltage variation	±10%	±10%
Combined variation of voltage and frequency	±10%	±10%
Locked rotor torque in per cent of full-load torque	160%	300%
Breakdown torque in per cent of full-load torque	200	None specified
Pull-up torque in per cent of full-load torque	140	None specified

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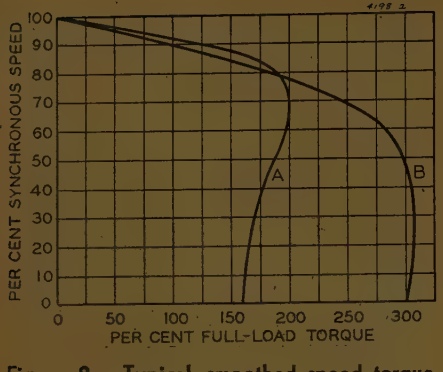


Figure 2. Typical smoothed speed–torque curves for induction motors

A. Continuous-duty motors
B. Intermittent-duty motors

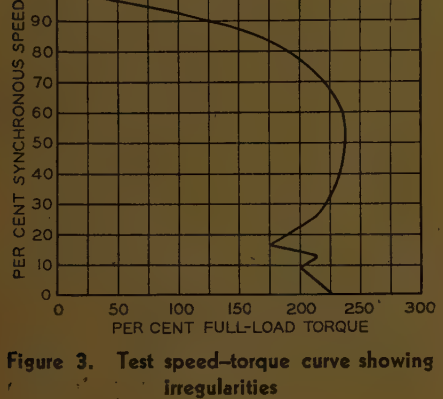


Figure 3. Test speed–torque curve showing irregularities

variations which would be recorded by oscillographic means. In using curves of this type it must be remembered that they apply only at rated voltage unless otherwise specified. When the voltage and frequency variations that are expected are taken into account, these curves vary as shown in Figures 4 and 5. Figures 4 and 5 give the maximum variation of the speed-torque curves for a variation of plus or minus ten per cent in voltage. This is the worst condition which can occur and still be within the limits specified by the Army Air Forces.

It should be emphasized that the curves included to illustrate general performance were obtained by assuming a certain set of constants for these motors. To make any application study a definite design curve should be obtained from the motor supplier.

Motor Operating Speed

The aforementioned curves have been plotted on the basis of per cent rated torque and per cent synchronous speed. For the 400-cycle system, the following motor synchronous speeds are possible: 24,000 rpm, 12,000 rpm, 8,000 rpm, 6,000 rpm, 4,800 rpm, 4,000 rpm, 3,428.50 rpm, and 3,000 rpm. These speeds are the synchronous speeds for 2-, 4-, 6-, 8-, 10-, 12-, 14-, and 16-pole motors, respectively. In general, a lighter power unit can be obtained by using the higher-speed motors and gearing down to the speed desired rather than using a motor with a large number of poles. Some idea of the change in weight with the change in the number of poles can be obtained when it is considered that an eight-pole one-horsepower motor has about $1\frac{3}{4}$ times as much active material as a four-pole one-horsepower motor. Nearly all three-phase induction motors run within a few per cent of their synchronous speed at full load. This can be seen easily by examining the curves in Figures 2, 3, 4, and 5. Further examination of these curves show that, even under conditions of high overload, the speed of the motor is still relatively close to the synchronous speed. For this reason an induction motor is known as a constant-speed type of power source. Where speed control is desired this constancy of speed in the

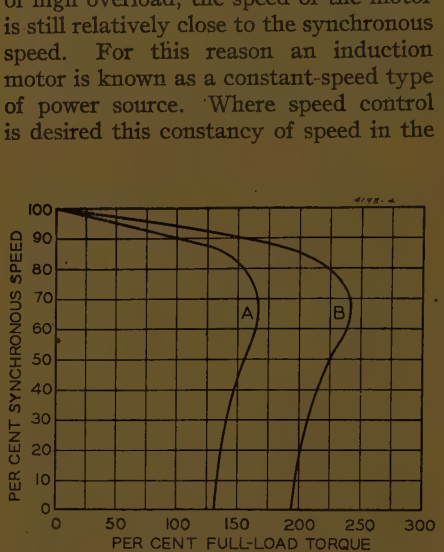


Figure 4. Speed–torque curves showing variation in torque due to voltage variation for continuous-duty motors

A. Speed–torque curve at 90 per cent voltage
B. Speed–torque curve at 110 per cent voltage

induction motor is one of its chief disadvantages. Speed is set by frequency and by certain constants in the motor, and therefore cannot be varied for an individual motor. Compare this constancy of speed with the speed-torque curves as shown for the main types of d-c motors in Figure 1. The series motor has a very large change in speed from no load to full load, and with overload the change becomes correspondingly greater. The shunt motor more closely resembles the induction motor. However, it has relatively large differences of speed between individual motors. This is not true of the induction motor, since its speed is regulated almost entirely by frequency. According to National Electrical Manufacturers Association Standards, the speed of a shunt motor at full load may vary plus or minus 7.5 per cent within a group of motors, whereas for the induction motor the speed variation among a group of motors is apt to be more nearly in the region of one half of one per cent.

In addition all d-c motors are subject to a speed variation with input voltage which may be nearly as great on a per centage basis as the per cent variation of the input voltage. Induction motors speed varies very little with the input voltage. Figures 4 and 5 show the approximate amount of variation that can be expected. However, the speed of an induction motor does vary with frequency, and, therefore, a variation of approximately plus or minus five per cent can be expected because of frequency change.

Locked-Rotor Inputs

Locked-rotor inputs affect the size of the feeders and protective devices and are, therefore, of interest to the aircraft-system engineer. It is impossible to give aircraft values applying to all cases, but the figures given in the Table II represent normal values that may be expected for three-phase 200-volt 400-cycle induction motors.

If, as often happens, the voltage under locked-rotor conditions is less than rated, the inputs will be reduced; the torque, kilowatts, and kilovolt-amperes will vary as the square of the voltage, the amperes will vary directly as the voltage, and the power factor will remain constant.

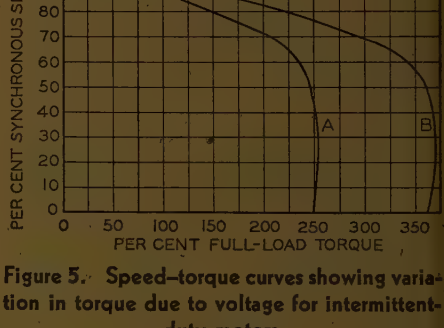


Figure 5. Speed–torque curves showing variation in torque due to voltage for intermittent-duty motors

A. Speed–torque curve at 90 per cent voltage
B. Speed–torque curve at 110 per cent voltage

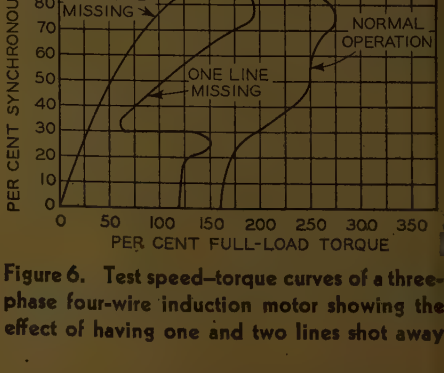


Figure 6. Test speed–torque curves of a three-phase four-wire induction motor showing the effect of having one and two lines shot away

Motor Temperature and Altitude

Most of the motors designed for use in aircraft are designed to be cooled by a self-driven fan. The amount of cooling depends upon the differential of temperature between the cooling air and the motor and upon the density of the air. Motors normally are designed to operate at various altitudes with temperatures per the National Advisory Committee for Aeronautics Standards charts. If the motor is subjected to ambient temperatures other than those given on these charts for the various altitudes, the motor must be more conservatively designed and therefore heavier in weight.

Motor Operation With One and Two Wires Shot Away

In the installation of d-c motors one wire and a ground is usually used. With this installation, if the one wire is shot away, the motor stops. The a-c motor has three line leads and the neutral grounded. If one or two line leads are shot away, the motor will still continue to run, in most cases, though it may not necessarily start if it is once stopped. Figure 6 shows the test speed-torque curves of a motor that was designed to drive a generator which was subjected to overloads of short time duration. This motor was designed, therefore, to have a rather high pullout torque point with respect to full-load torque. Tests were taken with one line open and two lines open. The results are shown in Figure 6. Note particularly the cusp that occurs at one-third synchronous speed with one line open. The size of this cusp varies with the winding of the motor. For some motors the torque actually may go negative at this point. If this occurs in a particular motor it would never accelerate to its normal running speed. A motor having the torque characteristic with one line open such as shown in Figure 6 would accelerate to full running speed providing the torque loading was light. With two lines open, the motor would be running on single phase power and would act as a single phase motor with no auxiliary winding for starting purposes. As long as it is running, it would continue to run provided it was not too heavily loaded. Once it stopped, it would not automatically start itself when power was supplied. A motor operating with either of the two conditions—one or two lines open—would not last long but would soon burn itself out or operate any thermal protection device that might be applied. Considering the multi-circuit wiring systems that have been laid out for the a-c systems, however, the chance of a motor operating with either of these two conditions is extremely remote.

Table II

	Continuous Duty	Intermittent Duty
Torque, in per cent of full load	160	300
Kilowatts per horsepower	2.4	3.5
Power factor	0.50	0.60
Kilovolt-amperes per horsepower	4.8	5.8
Amperes per horsepower	14	17

Motor Loads

Some of the other characteristics that affect the application of the motors can best be brought out by considering some of the different types of loads that occur in an airplane. Many of these loads do not occur singly but in combination. Because of the multiplicity of combinations, only the more common types of load will be illustrated here.

Consider first the fan type of load. This load has a torque characteristic which varies as the square of the speed. Figure 7 shows a continuous-duty motor characteristic with a fan-torque characteristic superimposed. It will be noticed that this motor at sea level is loaded to full load. Quite obviously there is no starting problem for a strictly fan load. But note what happens to the load on the motor at 30,000 feet. Since the induction motor is practically a constant-speed motor, the torque loading goes down as the density decreases. Compare this with the fan operated by a series motor as shown by Figure 8. Here, as the torque loading decreases with density, the motor tends to speed up, thus partially compensating for the decrease in density. Thus with two motors of identical rating the series motor would drive the fan in such a way as to deliver more pounds of air at high altitude. With the induction motor the load at 30,000 feet has dropped to approximately 48 per cent of the sea-level value, while with a series motor it has decreased to only 66 per cent. With a series motor even less drop in load can be obtained by operating on a steeper portion of the torque curve. If such compensation is desired with the induction motor, other means of control must be used.

Another type of load which is encountered very commonly is the so-called friction-type load. This type of loading is usually considered to have constant friction or torque over a large range of speed. If it is remembered that these friction loads generally are made up of bearings and gear losses, it can be seen that they are not constant but vary considerably with the consistency of the lubricant. Very few lubricants are not affected by extremes of temperature encountered in aircraft. Even the friction between two rubbing surfaces changes with temperature and the condition of the surfaces. Thus, the assumption that a friction load is a constant-torque load is false. Only if all conditions of temperature and surface are maintained constant can it be expected that the friction loads will be constant. In making any application of a motor to a friction load the maximum torque required should be deter-

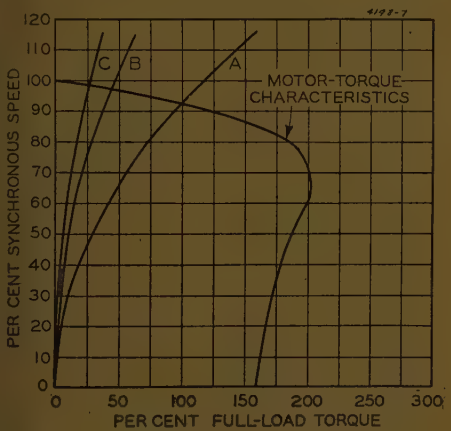


Figure 7. Polyphase induction motor driving a fan

A. Fan speed–torque curve at sea level
B. Fan speed–torque curve at 30,000-feet altitude
C. Fan speed–torque curve at 40,000-feet altitude

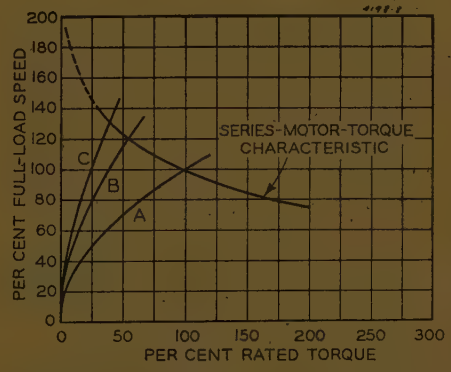


Figure 8. Fan load on a d-c series motor

A. At sea level
B. At 30,000 feet
C. At 40,000 feet

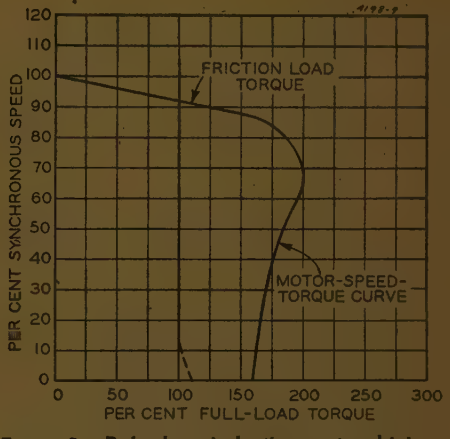


Figure 9. Polyphase induction motor driving a pure friction load

mined for the worst conditions of temperature and surface that can be expected. Most friction loads are characterized by having a relatively high breakaway torque, that is, the coefficient of friction is higher under static than under running conditions. Because of the relatively low starting torque of continuous duty induction motors this may become the limiting factor. Figure 9 illustrates a pure friction load with no allowance for variation due to temperature and surface conditions.

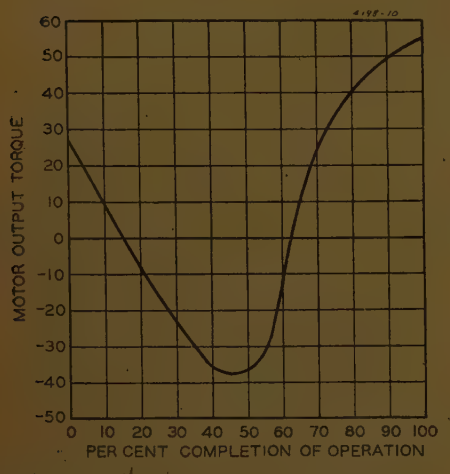


Figure 10. Motor torque in carrying out an intermittent-cycle operation having an overhauling load

Probably one of the most interesting types of application, not so much from a motor standpoint as from a control standpoint, is the operation of inertia-type loads. Gyroscopes driven by motors are used in many of our best weapons today. These units generally require very little running torque, since the only thing which tends to stop them is a slight amount of bearing friction and some windage about the outside surface of the gyro. Therefore, the tendency is to apply relatively very small motors to these units. Care must be taken, however, that adequate surface is provided to dissipate heat from the rotors of these motors. This can be understood easily if it is remembered that the amount of heat which must be dissipated in the rotor of a motor of this type is equal to the amount of stored energy in the gyroscope. Just as an example, consider a wheel having an inertia of one pound-foot squared being accelerated to 11,500 rpm. The energy stored in this wheel would be equivalent to 500 watt-minutes. To accelerate a wheel of this size to this speed, the rotor of an induction motor must absorb an equivalent amount of heat energy. This 500 watt-minutes is enough heat to raise the temperature of one pound of copper 175 degrees centigrade. The general tendency for applications of this sort is to use motors whose rotors weigh much less than one pound. In order to provide the necessary cooling the accelerating time must be extended, or additional cooling area must be provided for the rotor. One of the best tricks for application of this kind is to turn the motor inside out, so that the rotor becomes part of the rotating wheel and therefore has a large mass and heat-storage capacity.

Overhauling loads seldom are encountered by themselves but are generally a part of a cycle operation. Figure 10 illustrates a cycle of operation of an actuator in which an overhauling load occurs. The a-c induction motor is ideal for this type of application, since, if the motor is accelerated to only a few per cent above the synchronous speed, it will absorb approximately as much mechanical power as it would put out if it were running that per cent below synchronous speed.

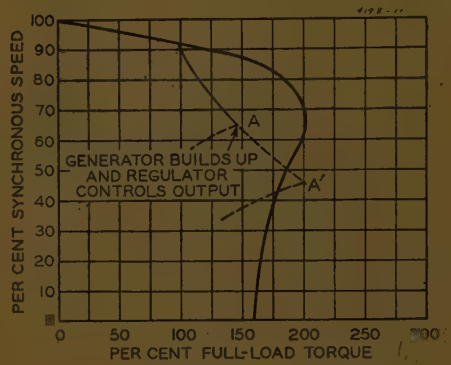


Figure 11. Regulated generator feeding a constant load driven by a three-phase induction motor

In other words, it becomes an asynchronous generator and feeds electric power back into the rest of the system. Motors handling loads of this type, of course, must be able to deliver sufficient torque to take care of the highest requirements of the cycle. Generally speaking, however, these cycles are of relatively short duration, and the cycles occur so infrequently that an intermittent-type motor can be used satisfactorily. The cycle illustrated in Figure 10 takes about 30 seconds.

A load which is encountered quite frequently and has many variations is that which makes the motor a driving means for a motor generator set. One variation of this is a motor generator set feeding a constant resistance load. Under this condition, as the motor speeds up, the generator builds up until it reaches a point where saturation occurs or a regulator takes control of the output voltage. When this regulator takes control, the output of the generator from there on is practically a constant amount of power. Under this condition the torque varies inversely as the speed. An examination of Figure 11 will show that a condition very easily could be encountered in which the motor generator set would build up to full voltage at a relatively low speed, and the motor would never accelerate beyond point A, as shown on the curve. If the generator built up to full voltage and the regulator took control at approximately 46 per cent of the synchronous speed of the motor, as shown on Figure 11, the point A would coincide with A' at 200 per cent of full-load torque. The motor would accelerate to about 50 per cent of full-load speed and run there. Another variation of the motor-generator-set application is one in which the motor generator set feeds a variable-type load, and the motor generator set is controlled in such a way that under starting conditions the maximum demand can be put on the generator. This type of motor generator set is often used in certain control circuits for actuating devices. Then there is the third type of motor generator set in which the excitation is constant. This motor generator set has a rather definite torque characteristic and does not present the problems which may occur with the others.

The preceding applications illustrate but a few of the many that occur on aircraft. There are a myriad of combinations of the fundamental loads that can occur. Each combination should be broken down and studied with respect to all of the conditions that may be encountered. Induction motors have been used in commercial practice for many years. Their simplicity, ruggedness, and lack of commutating or slip-ring brushes have given them an advantage which far outweighs the few disadvantages they may have. Similarly, in aircraft these same advantages will stand out more clearly with each succeeding application and their reliability and ease of maintenance when compared with the d-c motor with its brushes and commutator will lead to an everspreading use of the induction motor to do the hundreds of odd jobs that are of vital necessity to the modern aircraft.

References

1. Induction-Motor Slot Combinations, **Gabriel Kron.** AIEE Transactions, volume 50, 1931, pages 757–67.

2. Induction Motors, Performance Calculations, **C. G. Veinott.** AIEE Transactions, volume 51, 1932, pages 743–54.

3. Induction Motors, Polyphase, **W. J. Branson.** AIEE Transactions, volume 49, 1930, pages 319–28.

4. Modern Polyphase Induction Motors (book), **Punga Raydt.** Sir Isaac Pitman and Sons, Ltd., London, England.

5. Symmetrical Components (book), **C. F. Wagner, R. D. Evans.** McGraw-Hill Book Company, Inc., New York, N. Y.

6. Applications of the Methods of Symmetrical Components (book), **Waldo V. Lyon.** McGraw-Hill Book Company, Inc., New York, N. Y.

7. Applications of Electric Power in Aircraft, **T. B. Holliday.** *Electrical Engineering*, volume 60, May 1941, pages 218–25.

8. A 40-Kva 400-Cycle Alternator for Aircraft Service, **H. E. Keneipp, C. G. Veinott.** AIEE Transactions, volume 63, 1944, November section.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Aircraft Storage-Battery Design

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WITH the present great interest in aircraft, not only because of its wartime importance, but also because of the prominence it will have commercially after the war, as well as in postwar military needs, a careful scrutiny of the vital parts that make up the aircraft is in order. One of these parts is obviously the aircraft storage battery. It is used for engine starting, ignition, lighting, and radio.

The paper points out wherein the aircraft battery requirements differ from battery requirements encountered heretofore. Then step by step each of the important parts that goes to make the unit complete is described, and information is given as to what has been done in design to cause these parts to provide a performance of that part to meet aircraft usage.

Attention is called to certain departures that have occurred from what may be considered sound practice, and reasons are given as to how this occurred.

Suggestions are made as to methods of correcting these departures and of experimental work that is being carried on for further improvement.

In summarizing the data and experience accumulated, a suggested design is offered which should overcome the present deficiencies and meet present requirements.

Requirements

The design of aircraft storage batteries involves two major considerations which differentiate the problem from batteries for ground or marine uses. There is obviously first of all weight, and second a nonspill feature which will permit the battery to function at least for a limited time in any position.

Storage batteries usually are associated in one's mind with great weight, but since they are one of the best-known means for the storage of energy, which immediately can be converted to electrical use, intense effort and attention have been directed toward reducing the weight consistent with required service. The resulting watts per pound of weight of the battery compares favorably with other units on the aircraft.

Methods for Weight Reduction

The most apparent and fertile field for work on weight reduction has been the reduction of life expectancy of the battery down to a reasonable point for aircraft use. Like a shoe, the wear of which is proportional to the thickness of the sole, the wear in a battery is proportional to the thickness of the plates. Where life expectancy had been considered in periods of from 5 to 15 years, to consider it in from months to a year allowed much latitude for weight reduction.

Grids

The supporting framework for the active material of a lead storage battery consists of grids of antimonial lead. The long-life heavy battery will have grids of heavy section, and the antimonial content usually will be eight per cent. By increasing this content to 12 per cent, the eutectic point for this alloy, and by allowing specific limited amounts of impurities, such as tin, copper, and arsenic to be present, good castings can be made in very light sections and thicknesses. The grid life will be reduced, but not below a period of reasonable service. Figure 1 illustrates the difference in thickness and member sections of a battery grid designed to give $4^1/_2$ years of life and an aircraft battery grid.

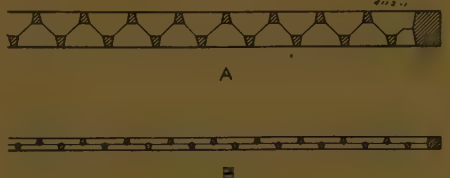


Figure 1

A. A typical positive-grid section designed to give $4^1/_2$ years of life in reasonable heavy service

B. An aircraft positive-grid section. The lighter section and thickness are obvious

Advantage is taken of the fact that, while the lead peroxide of the positive plate tends to distort its supporting grid by buckling and expanding it, thus requiring a sturdy construction, the sponge lead of the negative does not. The negative grid, since it need only support this sponge lead and act as a conductor, may be constructed of light members with horizontal bars far apart and verticals light in section.

Because aircraft batteries may be charged on a stationary bench in contrast to the vibration they receive in a plane, the practice of including feet on the grids as is done in many other types of service is continued. This permits the two feet of each plate of a given polarity to rest on the same bridges as the rest of the plates of that polarity, while the two feet of the plates of opposite polarity rest on alternate bridges. The sediment that clings to the bridges will in this manner be unable to cause short circuits between plates (see Figure 2).

Active Material

The finished active material of the positive plate is lead peroxide (PbO_2). This

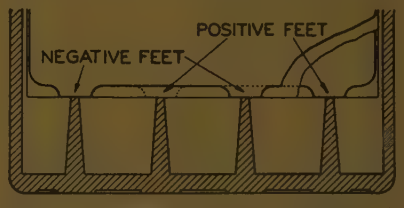


Figure 2. There are four bridges. The feet of the positive plates (two each) rest on alternate bridges. The feet of the negative plates rest on their alternate bridges

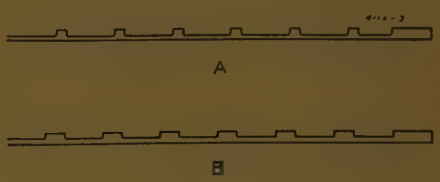


Figure 3

A. The profile of a microporous separator

B. The profile of a wood separator

Since the former has greater mechanical strength, the ribs may be narrower allowing more open space between them and greater acid in contact with the plate

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Figure 4. The cross section is constant, decreasing the material used. The ohmic resistance is low because there are no thick sections

is produced by mixing with a solution of sulphuric acid and water an oxide of lead, usually red lead (Pb_3O_4) or litharge (PbO) or both, and further oxidation occurs when this plate serves as an anode in an electrochemical bath using sulphuric acid as an electrolyte. The mixture of oxide and acid solution is made into a heavy paste and applied to the grids with a hand paddle or by a special machine. A wide variation in porosity may be obtained by changing the proportions of red lead and litharge used, the weight of these oxides per unit of volume, and the specific gravity of mixing acid used. Since an aircraft battery is called upon to deliver high currents for short intervals, an open porous peroxide is needed. Therefore, a lightweight red lead usually is selected and mixed with a rather high specific-gravity acid. The resulting large and numerous pores give ready access of the cell electrolyte into the plate. The reaction of the active material with the electrolyte is immediate, and large currents can be delivered.

Obviously this highly expanded peroxide is of less weight than denser ones, which assists in keeping the battery weight down. Active materials like this are of short life compared to dense ones, so the material must not be expanded to a point where it will shed out and fall uselessly into the sediment wells before a reasonable battery life has been obtained.

The finished active material of the negative plate is sponge lead (Pb). Litharge, usually a rather heavy one, is mixed with sulphuric acid pasted into grids to make plates and electrochemically reduced to sponge lead. To "sponge out" the lead, so that the cell electrolyte will have ready access and to further aid in the output of the battery, a small percentage of lamp black and barium sulphate is added. The carbon serves mostly to increase the sponginess but also aids as an electric conductor; the barium sulphate changes the crystalline structure, keeping the crystals small and finely divided. Certain organic materials in small quantities are added to increase the battery output at low temperatures. Among these are wood dust, lignin, hemp, cotton, cork, and humic acid. They are used in quantities of only a fraction of one per cent by weight. Seemingly, they take no part in the reaction, but on discharge will increase the battery output at −20 degrees Fahrenheit to five times that obtainable without them.

Separators

Separators used are microporous rubber or treated wood. Either may be used in conjunction with a glass mat. The mat does not act as a separator but as a retainer of the positive active material. It is applied between the grooved side of the separator and the surface of the positive active material, thus holding mechanically in place material that otherwise would become dislodged and fall into the sediment well. Its use is recommended where long life is desired with but a minimum of sacrifice of high-ampere-rate performance. Since the mats are only 0.03 inch thick and can contain electrolyte up to 90 per cent of their volume, the slight additional internal resistance which they offer is almost offset by the more immediate access they permit of the electrolyte to the positive active material. The use of glass mats may be expected to add 50 per cent to the life of the battery.

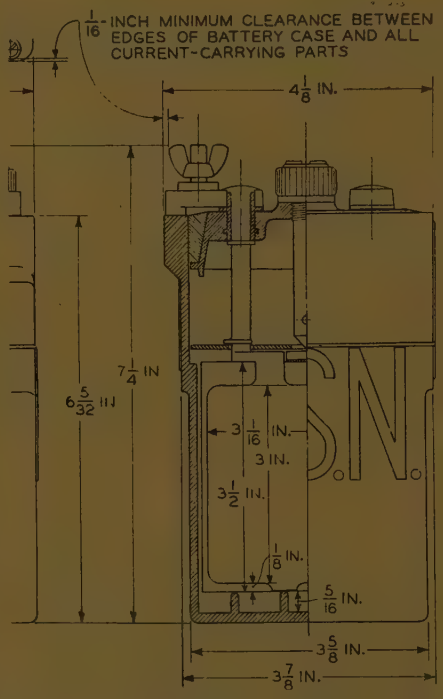


Figure 5. Liberty-motor ignition battery

The space above the electrolyte will hold all of the electrolyte when battery is on its side without submerging vent plug. When upside down, electrolyte will not submerge hole in end of vent tube

Microporous-rubber separators are preferable to wood. They more nearly balance out the life of the separator to the plates than do the wood separators. Their cost is higher than wood, but the increase represents only about five per cent of the cost of the complete battery.

Additional advantages are:

1. The lower ohmic resistance per unit of projected area results in lower internal resistance of the cell and consequently higher cell voltages on discharges.

2. Since they do not require treatment before use, they may be inserted into position between the plates in a dry state.

This permits the battery to be stored dry for an indefinite period without deterioration. The battery can be filled with electrolyte and charged wherever needed with no loss in performance or life, regardless of the storage period.

Batteries insulated with moist wood separators and stored deteriorate seriously after six months of storage.

The wood separator, so far as kind of wood, general profile, and shape, follows that conventionally found in automobile batteries, except that, because of weight and space limitations, it must be made in thinner sections. Preferred wood is cedar, but fir, cypress, and redwood are used. It may be sawed, rotary-cut veneered, or sliced.

To remove natural resins which are harmful to the plates, the wood, after being grooved, is treated in a three per cent solution of caustic. This also serves



Figure 6. The present German construction is little different from Figure 5. The visual level indicator (rectangular glass plate) is unique

to decrease the ohmic resistance, but after treatment the separator must be kept moist or it will develop a high resistance and become weak mechanically exhibiting splits and cracks.

The usual practice in either microporous rubber or wood is to allow an 0.030-inch back web and a groove 0.040 inch deep. The purpose is to make the separator resistance low and to allow as much electrolyte as possible to come between the plates for ready reaction with the active material. Conventional profile of the separators is shown in Figure 3.

Recent experiments on a microporous separator of a profile as shown in Figure 4 shows promise. Here the cross section through the separator has been kept constant. A 20 per cent decrease in ohmic resistance is achieved. The shape lends itself more readily to the manufacture of synthetic rubber. The volume and weight of material used is lessened by one fifth. As far as tests have progressed results have been good both as to life and discharge. The separator has been produced only on a laboratory basis.

Electrolyte

Sulphuric acid with a specific gravity when the battery is fully charged of 1.285 is used. Permissible impurities in this specific gravity are:

Impurity	Per Cent	Impurity	Per Cent
Iron	0.006	Nitrates	None
Arsenic	0.00005	Ammonia	0.00600
Antimony	0.00050	Chloride	0.01200
Manganese	0.00002	Copper	0.00500

Mechanical Construction

The first requirement for an especially designed lead–acid storage battery for aircraft occurred during World War I with the development of the Liberty motor, where it was desired to supplement the magneto with battery ignition. The battery designed and used was a small eight-volt one capable of delivering three amperes for three hours. Because many of the features found in it are still desirable, it is shown in section in Figure 5.

It will be noted that the nonspill feature was obtained by enlarging the electrolyte expansion chamber sufficiently, so that when the battery was placed on its side the electrolyte that ran from the cell proper was insufficient to rise to the vent plug in the center of the cell. When the battery was inverted the electrolyte was insufficient to rise to the hole in the lower end of the vent tube. Thus at all times the vent remained open for the escape of gas, but the electrolyte could not at any time reach the opening and run out. Since the periods during which the battery was inverted were short, sufficient acid was retained in the plates and separators, so that the reaction could continue and the battery deliver current. The use of glass mats even further aids under these inverted conditions.

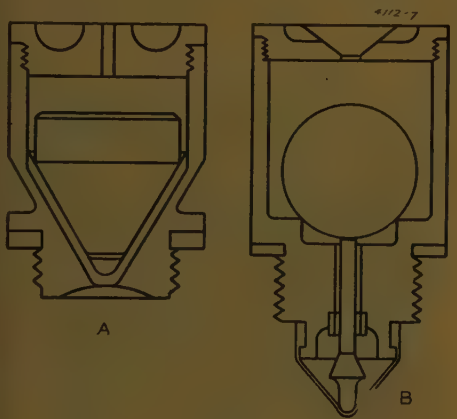


Figure 7

A. The lead weight slides along the inside conical surface of the plug body until it closes the hole in the plug top

B. The lead ball rolls off the valve stem permitting the rubber "spring" at the bottom to close the rubber-seated poppet valve

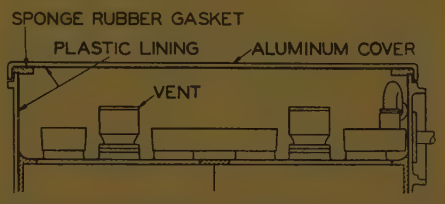


Figure 8. Electrolyte spilled from the vent is confined in the space enclosed by the plastic-lined aluminum container and lid

This space also contains the intercell connectors and terminals, which may become short-circuited if there is much acid

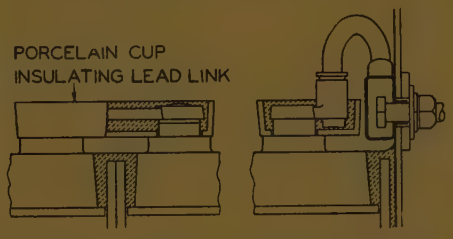


Figure 9. Intercell connector fitted with porcelain cup which is filled with asphaltic compound

The only disadvantages of this type of construction are:

1. The difficulty in obtaining a specific gravity reading, as the normal electrolyte level is well down below the vent plug opening in the cover.

2. The nonspill space required increases the cubical dimensions per ampere-hour as compared with conventional types.

It does not appreciably increase the weight per ampere-hour.

Specific-gravity readings may be taken by placing the battery on its side with vent plug removed, and by using a hydrometer syringe with a curved tube, so that the syringe body is in a vertical position when the tube end is immersed in the electrolyte.



Figure 10. The junction box containing the terminals must of necessity be small, making the application of the cable lugs to the battery terminal studs difficult

It is considered that this construction is still the most effective nonspill one developed.

Figure 6 shows the present German construction. It is interesting to note that they have adhered to this old construction but have added an electrolyte level indicator. The indicator consists simply of a strip of glass extending through the cover down to the normal electrolyte level. In ordinary daylight or with average lighting, the lower end of the glass appears as a greenish-white rectangle upon peering down through its upper end, that is, if the electrolyte is below normal level (the end of the glass strip). When water is added through the vent hole and as the level rises until it touches the lower end of the glass the greenish-white rectangle disappears leaving only darkness. The operator thus knows that the normal level has been reached.

An improvement on the German method has been made by using a rod of methylmethacrylate. By tapering the lower end to a cone and by observing the ensuing dark circle that appears as compared in size with the rod circumference it is possible to judge how far up the cone the level has risen. In this way a level "range" is obtained instead of simply "below normal" or "normal or more."

In passing, comment is made that German lead–acid aircraft batteries compare unfavorably with United States batteries in ampere-hours per pound of weight. This is because of design and not forced substitution of materials. Their principal loss is in their grid construction

For 15 years the desire of plane users has been away from the construction previously described to a battery having only the normal necessary expansion space and the use of vent plugs with check-valve arrangements which, upon rotating the battery, would close before the electrolyte reached them. This does result in a reduction of over-all height, but not a material one, as in incorporating the check-valve arrangement, whatever it may be, additional height must be given to the vent plug.

Figure 7 shows two types that have been used widely. In one case the inverted lead cone slides along the conical surface of the vent-plug body until it comes in contact with the vent top closing the vent hole. This should occur before the electrolyte reaches it. In the other the lead ball rolls off the hard-rubber valve stem allowing the rubber band at the bottom of the plug to force shut the poppet valve mounted on the stem. Even lead is subject to some corrosion by sulphuric acid, making parts stick and become inoperative. Disassembling the plug and cleaning it or discarding it for a new one becomes necessary.

A major objection to the check-valve vent plug is its failure to allow the venting of gas when closed. Some gas is given off from the battery on discharge, and a great deal is given off on charge, the amount being dependent on the amperage of the charging current and the state of charge of the battery. Present generators have extremely high amperage output, and it is not expected that the comparatively small batteries used can absorb it. Voltage regulators are used to control the input to the battery. Should these regulators become inoperative even for a short time, the gas evolved by the battery, even though it is in a partially charged condition, is enormous, and the vent plug has difficulty to relieve it when in an open position. If closed, a pressure is built up quickly which can force the cell cover out of the container, break the seal, and allow complete spillage of the acid.

To collect electrolyte spillage from nonfunctioning vent plugs, the aluminum boxes and covers in which the batteries are encased to provide radio shielding are lined with soft rubber or an acid-proof plastic. A rubber gasket is applied between the aluminum case and cover, so that, when the hold-downs are drawn up, a seal is obtained which will hold any electrolyte spilled out of the battery proper in the open space above the battery. Figure 8 shows the general construction of aluminum box, cover, acid-proof lining, and space where the electrolyte collects. In many instances the accumulation of electrolyte in this space has been so great that drainage tubes have been provided in the aluminum container, so that the spillage can be collected in a separate container away from the battery.

The accumulation of acid on top of the battery may cause the battery to discharge through it from terminal to terminal and cell to cell. All terminal connections inside the aluminum box are covered with rubber or plastic coatings and in some installations the intercell connectors are imbedded in an asphaltic compound held around the connector by means of a porcelain cup (see Figure 9). The objections to this arrangement are the difficulty of removing a connector to open up a cell for examination or repair and the undesirable heat insulation of the connector. Because of the need for light weight, the cross section of the lead connectors is kept small.

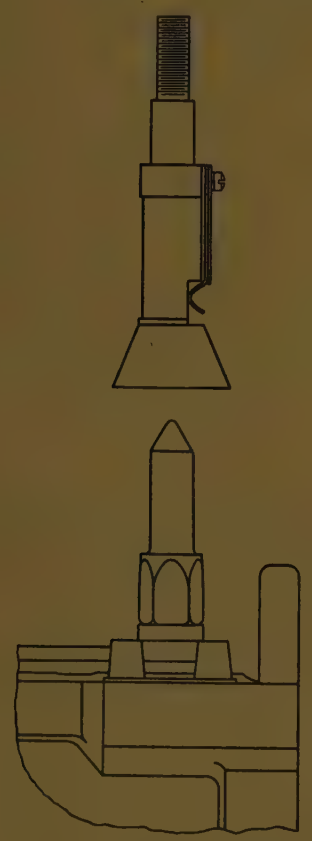


Figure 11. This push-on terminal holds promise

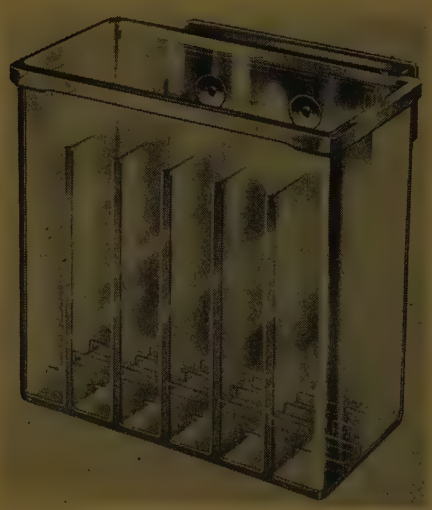


Figure 12. Instead of separate aluminum box, acidproof lining, and internal hard-rubber monobloc, the unit is in one polystyrene piece

Outside Terminal Connections

The present practice is to bring the battery terminals through the aluminum box by means of lead-coated brass studs encased in acid-proof nonconducting bushings drawn down with nuts. The cable ends of the plane harness to the power source and battery load are supplied with terminal lugs and are attached to the protruding studs and held with wing nuts. This outside assembly is encased in an aluminum junction box for radio shielding. The cables enter the box through knockout holes to which the cable shield is attached (see Figure 10).

While this is an effective connection, confined space often makes it awkward as the lid of the junction box must be removed to reach the wing nuts. Other interfering equipment on the plane may make these wing nuts difficult to reach.

Consideration is being given to quick-acting positive connections, such as is shown in Figure 11. It is believed that there will be no weight increase, and that a better and more effective connector will result.

Cell or Battery Container

Hard rubber of good quality having an elongation of six per cent and a tensile strength of 5,000 pounds per square inch has been used for aircraft containers since their inception. It is sufficiently nonacid absorbent to outlast the battery plates. It is reasonably light in weight, does not soften until aforementioned temperatures encountered in satisfactory battery use, and does not become too brittle at low temperatures. Its rigidity



Figure 13. The polystyrene assembly plated with 0.001 inch of lead for radio shielding

is high enough so as not to distort when subjected to hold-down pressures sufficient to hold the battery in place. Could hard rubber of the quality described continue to be available, it is doubtful whether any serious search would be made for a substitute, but the scarcity of natural crude rubber has necessitated dropping the elongation to three per cent and the tensile strength to 4,000 pounds. This has brought about some weight increase.

Much research has been done to find a substitute. Cellulose nitrate has the necessary characteristics for a battery container and long has been used as such. It can be fabricated easily without large tool and mold costs and is plentiful, but it is highly inflammable and hazardous to work with during manufacture and use.

Hydrolized wood has been given attention and is still being investigated.

The most promising material is the plastic polystyrene. It has all of the desirable features of hard rubber plus the fact that it may be used in thinner sections, resulting in a sizable saving in weight. The present problem is not the material itself nor any lack of physical or chemical requirements, but rather the problem of molding.

Polystyrene is a thermoplastic and injection molding of battery cases in sizes smaller than are necessary in aircraft and has been quite successful. Many thousands are being built or are already in use. They are light in weight, transparent (so that the physical condition of the battery may be observed); covers are cemented to the container and become an integral part of it (making sealing leaks impossible), are cleaned easily, thus avoiding external short circuiting.

Aircraft batteries of this material have been built wherein the polystyrene replaced the aluminum box, acid-proof lining, and hard-rubber cell container in one piece. The resultant weight saving was several pounds on a 12-volt 34-ampere-hour battery. All metal in the box construction that might be subject to acid corrosion was eliminated.

The weakness in this polystyrene unit assembly came from the fact that, at the time the work was done, no press large enough to make the box with one injection was available, and from four to six injections had to be made to fill the mold. Strains in the unit resulted, and fractures occurred at low temperatures during vibration. It is believed that, when sufficiently large presses are available to handle a unit of this size with one injection, a conversion to this material and construction may follow. This will be governed also by the supply of available natural rubber or development in the technique of handling synthetic rubbers. Figure 12 shows the unit polystyrene assembly.

The main purpose of the aluminum box in present design is for radio shielding, although as has been pointed out, since it has been provided with an acid-proof lining, its upper portion together with its lid or cover acts as a container for spilled electrolyte. In the case of the polystyrene unit assembly the radio shielding was accomplished by electroplating the outer surface of container and cover with a coating of cadmium. Later lead was used, being preferable as it is acid-resistant and painting of the box was unnecessary. Both of these metals adhered excellently, and rough treatment such as blows with a blunt instrument and scratching did not loosen them nor cause them to peel. The coatings used were from 0.001 to 0.0015 inch thick and added but 2.5 ounces to the container weight; whereas, for the same size battery the aluminum box weighs 3.25 pounds.

Figure 13 shows the polystyrene unit assembly plated with 0.001 inch of lead.

Suggested Design

Many of the objections to present-day aircraft designs are the result of rapidly changing conditions, causing the adoption of one expedient after another. To overcome one weakness some stopgap was used, which in turn had its own drawbacks, and additional adaptations had to be made to correct it.

As a postwar design, or as a war design if materials and tools become available, the following is suggested:

1. Revert to the old nonspill method using the large electrolyte expansion space and the tube vent plug. It will not spill, and gas is vented during inversion.
2. Provide a rod-type visual acid-indicating device.
3. Use a simple positive quick-acting push-on terminal connection.
4. Manufacture the container complete of hard rubber (if available) or polystyrene plastic. If the latter is used, since it is transparent, the visual indicating device may be eliminated, unless radio shielding is necessary.
5. Definitely determine whether radio-battery shielding is necessary. There has been some doubt on this point.
6. If necessary accomplish the shielding by electroplating.
7. Also, if necessary, provide a simple hard-rubber or plastic lid electroplated and clamped to the battery top.

A design such as this will be light in weight, actually nonspill, easy to install and remove, and readily connected to the plane wiring. Figure 14 is a suggested general design.

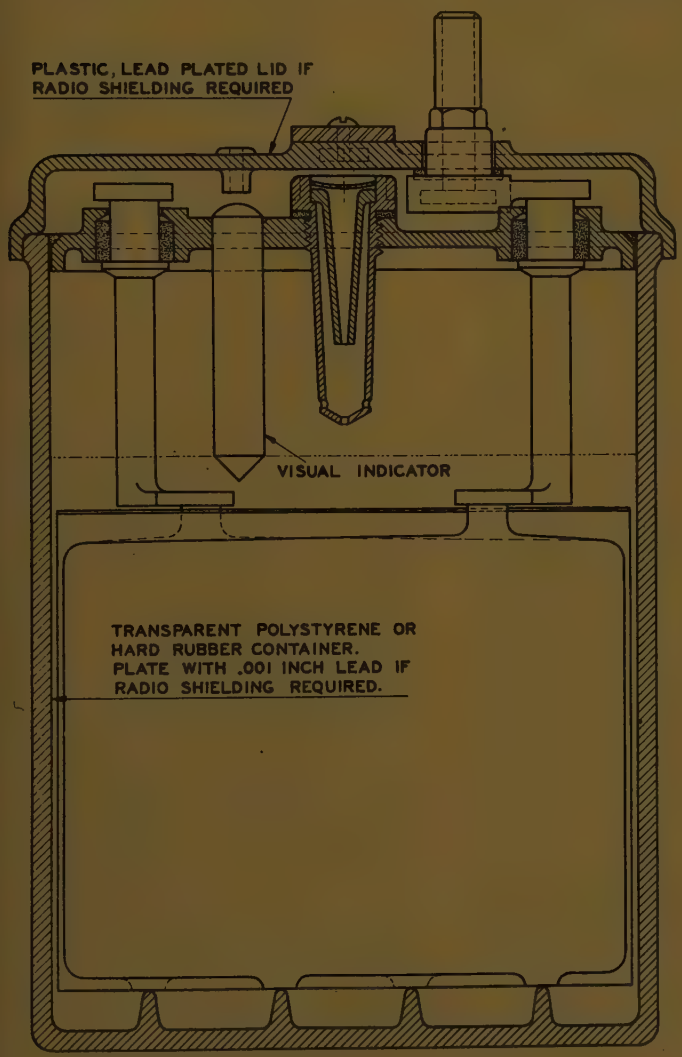


Figure 14. This design suggests plastic container, plating for radio shielding, tube-type vent for nonspill, push-on terminals, and visual indicating device

Rating of Electric Equipment Under Intermittent Load

MYRON ZUCKER
MEMBER AIEE

Synopsis: Resistance welding furnishes a clear-cut case of intermittent load. The more elaborate and expensive equipment that marks the trend in welding away from a blacksmith's art to a carefully controlled science warrants increased care in design. This paper explores one aspect of this design: the thermal limits to the periodic load that can be carried. In this case the application is to resistance-welding problems, and illustrative material is drawn from a type of interleaved flexible welding cable that has seen seven years' service in industry. The principles, however, apply to all types of welding: in fact, they extend to many fields in which the problem is to find the reaction to an intermittent force—such as electrical or mechanical transients.

Analysis of Ratings Under Intermittent Load

Shortcomings of Duty Cycle

WHEN electric equipment is subjected to intermittent load, as in spot-, flash-, projection-, or gun-welding, it is customary to rate it according to the duty cycle[1,2] under which it is to be used. This is good practice as long as we take duty cycle for what it really is: the proportion of *time* that the equipment is heated—or current "on" time divided by total time. But we may get into trouble by extending this definition to say that *temperature rise* is proportional to duty cycle—as is commonly done in rating welding equipment for intermittent load.

In the past, uncertainty of design has called for such large safety factors in welding equipment that the errors inherent in using duty cycle as a load multiplier were unimportant. The advance in the art justifies a review of this situation. Such a review shows that for periodic loads there is a more accurate—and, in many cases, an easier-to-use—method for calculating maximum temperature of the machine.

There are two indictments against duty cycle and its associated averaging time:

1. It does not properly account for the individual heating and cooling curves.

2. It does not properly account for the grouping of impulses.

These two errors work in opposite directions. They frequently compensate each other to a great degree, giving a passable result—which may be the reason why duty cycle has worked as well as it has.

Before undertaking the mathematical analysis of heating under intermittent load, let us get a graphical picture of the errors in duty cycle by studying the temperature reached when load is periodically applied and removed.

Individual Hit Cycles. Figure 1 shows two temperature–time curves for two single hit cycles of different durations but the same *proportion* of "on" time. Contrast the maximum temperatures. Both cases have 33.3 per cent duty cycle, and, according to conventional practice, are considered to impose the same service on the equipment—which they obviously do not. That is the first error.

Groups of Hits. The second weakness concerns series of time patterns such as Figures 2, 3, and 4. For the simplest case (Figure 2, an on–off routine followed for an indefinite time), duty cycle may give nearly correct answers if used within proper limits. But Figures 3 and 4, which are more nearly typical of actual operation because of the rests between limited numbers of hits and groups of hits, is not so well adapted to duty-cycle calculation.

The trouble in this case centers about maximum averaging time over which load is to be averaged when duty cycle is computed. The maximum averaging time assigned to each piece of equipment* is intended to reflect the lower temperature found under intermittent load in equipment having greater heat storage. This intention is correct in a general sort of way.

But it is a chore to use, since it is necessary to draw a picture of the welding operation[2] and slide a maximum averaging time along the time axis to find the part of the picture that includes the most "on" time within the maximum averaging time under consideration.

This limitation on averaging time not only is cumbersome, but gives inconsistent results. It is natural to expect that, as averaging time increases, duty cycle should decrease continuously. But it does no such thing!

Figure 5 illustrates how duty cycle violates the preceding expectation for the pattern of Figure 3 averaged over a wide range of averaging times. The values of duty cycle reach a minimum at multiples of six seconds, stepping up erratically after each multiple, preparatory to a swoop down to the next. An interesting picture, but an absurd standard by which to rate expensive equipment.

Summary. We have, therefore, grounds for questioning the whole duty-cycle procedure. In fact, we will see later that:

1. Duty cycle may be inaccurate by large percentages even in the simplest cases, because:

(a). It does not account for the characteristics of heating.

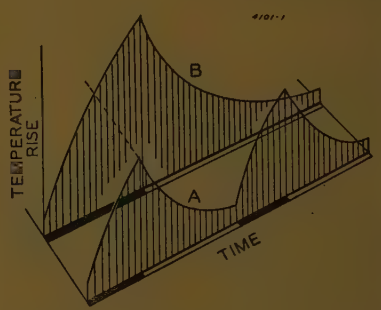


Figure 1. Duration of hits affects peak temperature, even though duty cycle is the same

Curves *A* and *B* show temperature rise on one piece of equipment for two cases where "on" time (indicated by black bars at the base) is one third of total time. Because of longer duration on *B*, its temperature rise is higher than *A*

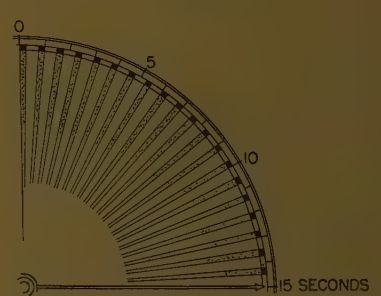


Figure 2 Timing of continuous hits

The timing is described by "on" time 0.25 second, and either:

(a). Off time = 0.5 second
(b). Total time (duration of hit cycle) = 0.75 second
(c). Rate of hits = 80 per minute

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* There seems to have been no definite method of specifying averaging time. One of the few statements in this regard is made by Pike and Ulrey,[3] in discussing the portion of electronic-tube rating that depends on temperature: "The period over which current is averaged must be short enough that the highest temperatures reached will not be in excess of that obtained if steady current equal to the average current rating is kept on continuously." This really requires a different averaging time with each load pattern.

(*b*). Its basic mathematical assumptions are correct only in certain cases.

2. Duty cycle and its associated averaging time are meaningless, inasmuch as they do not properly account for grouping and series of groups of impulses.

In addition, the duty cycle, even in cases where its poor assumptions are acceptable, can be justified only on the basis of maximum temperatures reached —rather than any weighted average such as has been found necessary in rating other equipment.[4] At the present state of knowledge, this point is only academic in welding problems.

Besides these scientific objections, there are some practical ones: cumbersomeness of application; conflict between this use of the term, "duty cycle," and the AIEE definition as applied to circuit breakers; as well as confusion in terminology between duty cycle and heating cycles consisting of so many (60th-of-a-second) cycles on and off. In an effort to clarify the situation, terms such as "per cent duty"[2] have been used. A change in nomenclature, however, is not enough: the procedure requires complete overhauling.

Duty Factor: Its Use and Meaning

Fortunately, there is a duty factor that is not only easy to use, but flexible, consistent, and correct within engineering limits. This duty factor is the product of a hit factor and a group factor. The partial factors are found from some simple charts, the use and derivation of which will be shown.



Figure 3. Timing of groups of hits

Hits of Figure 2, with three-second rest after each group of four hits: that is, one group each six seconds. "Active" time includes four complete hit cycles, that is, 4×0.75=3.0 seconds



Figure 4. Timing of series of groups

Four groups of Figure 3 repeated every minute. This pattern is duplicated, starting at $t=0$, like a clock. "Active" time=4×6.0=24 seconds

Working Charts

In computing duty factor, the first step is to find the hit duty factor F_h, as though hits were being repeated continuously. This value may be found from either Figure 6 or Figure 7, depending on whether the problem is set up in terms of rest time or of total time or welds per minute. Suppose, for instance, that hits have 0.25 second "on" time and 0.75 second total time (that is, they are made at the rate of 80 per minute). Figure 7A shows that

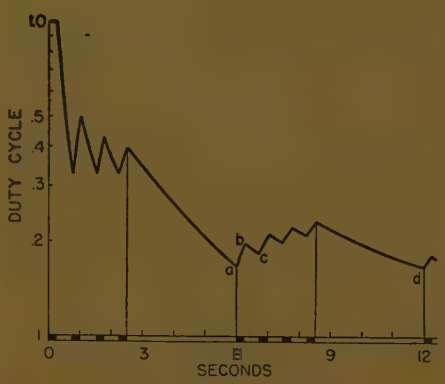


Figure 5. Duty cycle affected erratically by maximum averaging time

For the load pattern of Figure 3, conventional duty cycle sometimes shows big changes (*a* to *b*) for small difference in averaging time used in computation; other cases (*a* to *d*) show no change for large difference in averaging time. Duty factor avoids this inconsistency

the duty factor for this combination is 0.33: the answer to the case of Figure 2. For Figure 3, a second step is taken.

The second step is to find a group duty factor F_g by taking the *active time* as equal to the *number of hits per group* times *duration* of hit cycle. For example, with four of the aforementioned hits in a group, the active time is 4×0.75=3.0 seconds. Using this value, together with total time=6.0 seconds; we find from Figure 7B that the group duty factor is 0.55.

The complete duty factor that measures the temperature to which the equipment will rise under this load, compared to what it would reach under continuous load, is $F=F_hF_g=0.33\times0.55=0.182$.

A similar computation would be made if rest time were used instead of total time. That is, for the same pattern:

1. For hits, we have "on" time=0.25 second, rest time=0.5 second. From Figure 6A, we find $F_h=0.33$.

2. For groups, "active" time=3.0 seconds, rest time=3.0 seconds. From Figure 6B, we find $F_g=0.55$.

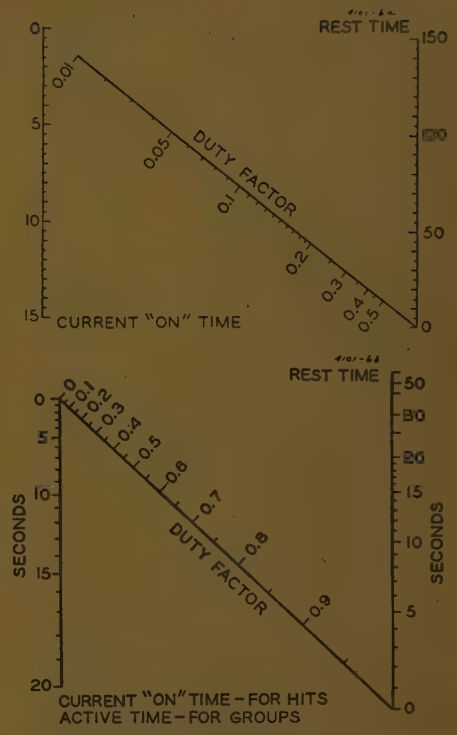


Figure 6. Rest time versus current "on" time versus duty factor, showing fraction of continuous-load temperature rise reached when intermittent load is applied

A. *For short periods.* In welding, this chart usually will be used for single hits, or for a group of hits in pulsation welding

Time scales may be multiplied by any factor provided *both* scales are multiplied. Duty-cycle scale is not changed

For the example given in the text, a straightedge is placed between current "on"=2.5 (that is, 0.25 times a scale multiplier of 10) and rest time=5.0 (that is, 0.5×10). Straightedge then intersects duty-cycle scale at 0.33, which equals *Fh*

This chart may be used directly for equipment having other heating time constants, if periods do not exceed ten per cent of the heating time constant of the equipment involved

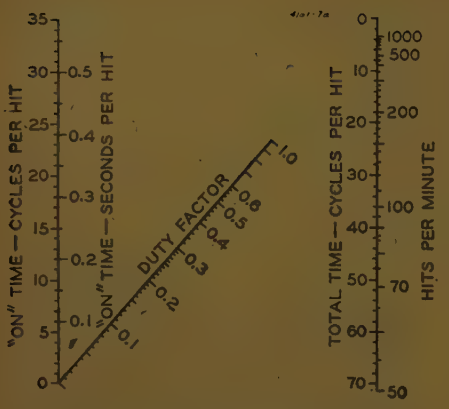
B. *For long periods.* This chart is adapted to groups of hits when there is a rest while changing from weld to weld; or to series of groups when there is a long rest while work is being changed

This chart is designed specifically for interleaved kickless cable, heating time constant=10 seconds. For other equipment, multiply time scales on chart by 10, and use them as (seconds duration (/) time constant in seconds) scales

The group duty factor for the example in the text is obtained by placing straightedge between three seconds on the "active" time scale and three seconds on the rest time scale. The straightedge crosses duty-factor scale at 0.55

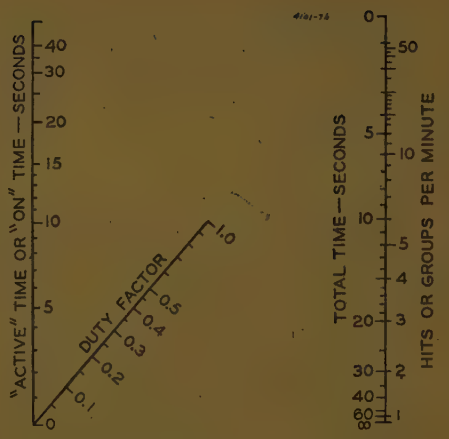
As before, complete duty factor is 0.33×0.55=0.182.

These results are for a light-duty kickless water-cooled welding cable, since the charts are designed for equipment having



A. For short periods. Like Figure 6A, this chart is designed for fast welding operation. "On" time may be measured as either cycles or fractions of seconds, since the two scales on the "total-time" axis are equivalent

Time scales may be multiplied by any factor, provided both scales are multiplied by the same factor. Duty-factor scale is not changed. For the example in the text, place a straightedge between 0.25 second on the left-hand scale, and 80 hits per minute on the right-hand scale. The "hit" duty factor is found to be 0.33, where straightedge intersects the diagonal scale



B. For long periods. This chart serves the same purpose as Figure 6B. It is also designed for kickless cable and is adapted to other equipment by

1. Multiplying time scales by 10 and using them as (duration per heating time constant) scales
2. Dividing the hits per minute scale by 10 and using it as a (hits per minute *times* heating time constant in seconds) scale

The example in the text is solved by a line between "active" time=3 seconds, and total time=6 seconds, which indicates a group duty factor of 0.55

Figure 7. Rate of welding versus current "on" time versus duty factor

a time constant of ten seconds which these cables normally have. For other equipment the same charts may be used by changing the time scales as instructed on the charts.

This procedure would be carried one step further if there were series of groups, as in Figure 4. For instance, if after four groups there were an interval of 36 seconds, series multiplier F_s would be obtained in a manner similar to the computation for group factor. In this case, $F_s=0.84$, making the over-all duty factor $F=F_hF_gF_s=0.182\times0.84=0.153$.

These answers may be compared with Figure 5. In this case the duty factor is near the lowest values of "duty cycle"; but this is not a generality—for in many cases the correct duty factor will exceed the duty cycle by a wide margin.

Meaning

The components of duty factor are measures of the temperature that would be attained if the corresponding cycle were to continue indefinitely. The effect of different degrees of continuity is illustrated in Figure 8. If a given load were supplied continuously, temperature rise eventually would be θ_f; if divided into hits, the rise would be only $F_h\theta_f$. (Illustration is for pattern of Figure 2 applied to equipment having such thermal relationships that under continuous load it takes one second for the temperature rise to reach one-half the ultimate value; the rate of rise for this load on ten-second equipment is too slow to permit picturing in space available.) But if, instead of being repeated continuously, hits are broken into groups, the group duty factor tells that only $F_hF_g\theta_f$ would be attained. Further, if there is a rest period between series of groups (Figure 4), we find that the temperature rises only to the fraction F_s of $F_hF_g\theta_f$. With this clue many combinations of periodic on–off patterns can be calculated.

Duty Factor: Derivation

Basic Equations

Finding duty factor for any combination of on–off cycles, really means to derive the maximum temperature under those circumstances.

To do this, we start with the basic exponential equation for temperature change in a single transient. The divergence of this equation from actual heating and cooling curves has been aired thoroughly in AIEE meetings[5] and elsewhere,[6,7,8] but still it is the only practical starting point. In fact, so many kinds of equipment do follow nearly exponential rules that perfectly satisfactory calculations can be made in many cases.

The expressions that have been used in studying the heating of power transformers and other apparatus under heavy load[4] are:

For heating, starting from zero temperature rise, $\theta=\theta_f(1-e^{-kt})$ (1)

Table I. Dynamic-Equilibrium Temperature Rises

21,000 Amperes on Light-Duty Cable for One-Sixth Second

Hits Per Minute	Maximum Temperature Rise (Degrees Centigrade) By Test	Calculated by Duty Factor	Calculated by Duty Cycle
3	10.0	10.7	6.3
6	17.1	16.8	12.7

For cooling $\theta=\theta_oe^{-kt}$ (2)

where

θ_f=*final* temperature expected from the constant heat input
θ_o=*initial* temperature at $t=0$
θ=temperature at any time t
$k=0.693T$

T=time required to produce half the ultimate temperature rise when load is applied continuously. It is called the "binary heating time constant."[5] The 0.693 is simply inserted to make equations 1 and 2 have the right values at the right times.

For the present analysis, since heating curves seldom start at zero rise, we will use a more general expression* that covers both heating and cooling from any temperature (see Figure 9):

$\theta=\theta_f(1-e^{-kt})+\theta_oe^{-kt}$ (3)

With this equation, we can find the maximum temperature rise in a given duty cycle, by *expressing the end temperature of each successive curve in terms of its initial temperature.*

Continuously Repeated Cycles

When a heating curve follows a cooling curve and vice versa (Figure 10), the initial value of each curve equals the final value of the other—that is, θ_o for a heating curve is θ_L, and θ_o for cooling is θ_H. Therefore, to find the temperatures at the end of the heating and cooling curves, we use these values for θ_o in equation 3, inserting a for the "on" time and b for the "off" time. This gives

After heating, $\theta_H=\theta_f(1-e^{-ka})+\theta_Le^{-ka}$ (4)

After cooling, $\theta_L=\theta_He^{-kb}$ (5)

* These equations assume that heat generated in the equipment is constant over the heating regime. This is a commonly accepted approximation for electric heating, close enough for most cases. When change of resistance with temperature causes material change in heat loss, or where electrical transients cause appreciable change in heat with time, a more refined analysis might be advisable. A partial solution, utilizing a variable time constant for the variable-resistance case, is made by Cooney[9] and Kennelly,[5] for $\theta_o=0$.

Another assumption—common to all applications of the simple exponential equation—is that heat dissipated from the equipment is proportional to temperature rise and that the body is isothermal. Otherwise, the analysis is too complicated for engineering use.[11]

Equation 3 has been developed in many forms. See, for example, reference 10.

To simplify the mathematical development and get usable working charts, we will express temperature rises as fractions of θ_f. Let $\theta_H/\theta_f=H$, and $\theta_L/\theta_f=L$. Then equations 4 and 5 become

$$H=1-e^{-ka}+Le^{-ka}=1-e^{-ka(1-L)} \quad (6)$$

$$L=He^{-kb} \quad (7)$$

We are now ready to consider the rise and fall of temperature as load is applied and removed periodically. If we keep the identity of each cycle (Figure 11), the initial temperature for the nth repetition of the heating curve is the final temperature for the previous cooling curve, L_{n-1}; the initial value for that $(n-1)$st cooling curve is the high point on the $(n-1)$st heating curve, H_{n-1}. Thus,

$$H_n=1-e^{-ka}+L_{n-1}e^{-ka} \quad (8)$$

$$L_{n-1}=H_{n-1}e^{-kb} \quad (9)$$

Then by combining equations 8 and 9, we find H_n, the peak of one cycle, in terms of H_{n-1}, the peak of the preceding cycle:

$$H_n=1-e^{-ka}+H_{n-1}e^{-ka}e^{-kb} \quad (10)$$

Equilibrium—if that is the proper word to use when there is a constant variation over a repeated pattern—is reached when successive values of H and L are equal: that is, when we can drop the subscripts. Then, solving equation 10 for H,

$$H=\frac{1-e^{-ka}}{1-e^{-ka}e^{-kb}} \quad (11)$$

and

$$L=\frac{1-e^{-ka}}{e^{kb}-e^{-ka}} \quad (12)$$

H is thus the duty factor F_h, for continuously repeated hits, in terms of on and off time. To express this in terms of on and total times, we make the common (though debatable) assumption that k is the same for cooling and heating. Then

$$e^{-ka}e^{-kb}=e^{-k(a+b)}=e^{-kc}$$

and equation 11 becomes

$$F_h=\frac{1-e^{-ka}}{1-e^{-kc}} \quad (12a)^*$$

Group of Hits

Let us now solve for the high temperatures, H, within a single group. (We omit low temperatures, because the high temperatures are generally used as a measure of the capacity of equipment.

* If instead of on–off cycle, there is a high–low load cycle,

$$H=\frac{1-e^{-ka}+Pe^{-ka}(1-e^{-kb})}{1-e^{-ka}e^{-kb}}$$

$$=\frac{1-Pe^{-kc}-(1-P)e^{-ka}}{1-e^{-kc}}$$

$$=\frac{1-e^{-ka}+P(e^{-ka}-e^{-kc})}{1-e^{-kc}}$$

where P is the ultimate temperature rise if the light load were applied continuously, divided by θ_f.

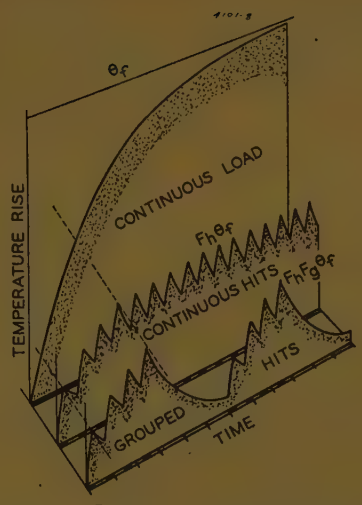


Figure 8. Cutting welds into groups has important effect on temperature rise

The difference is measured by the duty factors derived in the text

The effect actually is more marked than this drawing indicates, since to get the illustration within limited space, the "on" and "rest" times were made very long in relation to the heating time constant of the hypothetical equipment involved. This makes the temperature reach its maximum after a few hits, whereas in the numerical example given in the text the rise is so much slower that it takes six groups to reach 90 per cent of the final temperature

If at any time it becomes advisable to specify an effective temperature involving maximum and minimum values and their relative durations, the treatment should be extended to include this effect.) We will find H_n (Figure 11) by solving for each cycle in succession, starting with equation 8 to get the first cycle in terms of an arbitrary L_o, then using the relation of equation 10, with kc substituted for $k(a+b)$.

For the first hit,

$$H_1=1-e^{-ka}+e^{-ka}L_o \quad (13)$$

For hit 2, from equation 10 and 13,

$$H_2=1-e^{-ka}+e^{-kc}(1-e^{-ka}+e^{-ka}L_o)$$
$$=(1-e^{-ka})(1+e^{-kc})+e^{-k(a+c)}L_o \quad (14)$$

For hit 3, from equations 10 and 14,

$$H_3=1-e^{-ka}+e^{-kc}[(1-e^{-ka})(1+e^{-kc})+e^{-k(a+c)}L_o]$$
$$=(1-e^{-ka})[1+e^{-kc}(1+e^{-kc})]+e^{-k(a+2c)}L_o$$
$$=(1-e^{-ka})(1+e^{-kc}+e^{-2kc})+e^{-k(a+2c)}L_o \quad (15)$$

By continuing this process and comparing equations 13–15 and their successors, we can write the general expression

$$H_n=(1-e^{-ka})\sum_{m=0}^{n-1}e^{-mkc}+L_oe^{-k[a+(n-1)c]} \quad (16)$$

This can be reduced to a more usable form by letting $a+(n-1)c=d$ (see Figure 11), and by taking advantage of the fact that summation term is a geometric series starting with unity. The sum of such a series is $(1-r^N)/(1-r)$. In this case the ratio between successive terms, is e^{-kc}; and N, the number of terms to be summed, is n. Equation 16 becomes

$$H_n=(1-e^{-ka})(1-e^{-knc})/(1-e^{-kc})+L_oe^{-kd} \quad (17)$$

Continuously Repeated Groups

The relation that has been derived for temperature at the end of a group and that at its beginning permits us to link groups. Consider a time interval f between groups (Figure 12), and use subscripts d and $d+1$ to denote the ends of the last hit in respective groups, with g and $g+1$ marking the end of rests between groups.

Then at the end of the gth cooling interval, the low temperature is

$$L_g=H_de^{-kf} \quad (18)$$

whereas during the active period of the next group, from equation 17,

$$H_{d+1}=(1-e^{-ka})(1-e^{-nkc})/(1-e^{-kc})+L_ge^{-kd} \quad (19)$$

If we substitute equation 18 into equation 19,

$$H_{d+1}=(1-e^{-ka})(1-e^{-nkc})/(1-e^{-kc})+H_de^{-kd}e^{-kf} \quad (20)$$

Dynamic equilibrium comes between groups when $H_d=H_{d+1}$. Since this value is the ratio of *maximum temperature*

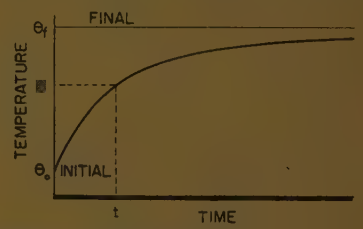


Figure 9. Exponential heating or cooling curve

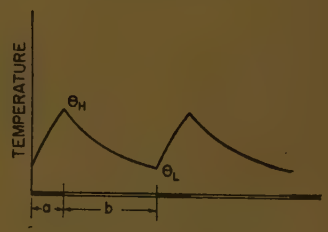


Figure 10. Hit cycle

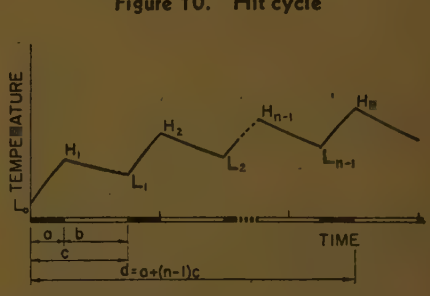


Figure 11. Successive high temperatures in groups of hits

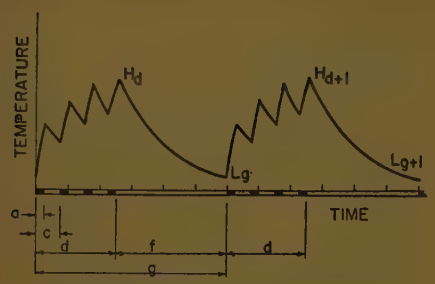
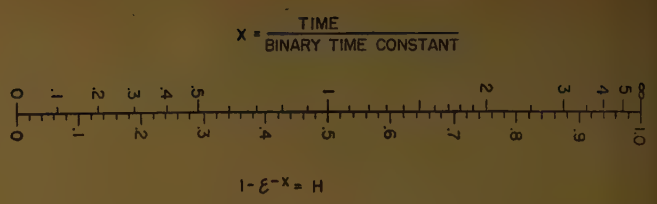


Figure 12. High and low temperatures in successive groups

Figure 13. Heating factor versus time



Values may be taken from this alignment chart, for solving equations 20 or 21

For values of x lower than 0.1, $1-e^{-x}=0.693x$

That is the range in which "duty cycle" is accurate for continuous hits

rise under intermittent load to temperature rise under continuous load, we may call it F, the *duty factor*. We find this by solving equation 20, and noting from Figure 11 that $d+f=g$:

$$F=\frac{(1-e^{-ka})(1-e^{-knc})}{(1-e^{-kc})(1-e^{-kg})}=F_hF_g \qquad (21)$$

Thus we have the complete duty factor. In computation, it may be thought of as having either two factors or four. If we use two, as was done in preparing the charts of Figures 6 and 7, the first terms in numerator and denominator combine to give F_h the hit duty factor; the second terms give F_g the group duty factor. Or the four values in parentheses may be taken from Figure 13 (or, for greater accuracy, from a modern table of exponential functions) and combined to find F.

This duty factor takes the place of duty cycle in the formulas for determining the periodic load that will heat equipment to its thermal limit. Thus, for example:

F. Permissible watts loss under periodic load = permissible continuous loss

$\sqrt{F}$. Rated current under intermittent load = rated continuous current

Tests on Kickless Welding Cable

The duty-factor theory has been substantiated by tests that checked:

(*a*). The reasonableness of the exponential-rate-of-change assumption in the case of kickless resistance-welding cable.

(*b*). The temperature at each peak during intermittent operation.

(*c*). Dynamic equilibrium temperatures during periodic operation.

The tests were run on water-cooled welding cables, which are convenient for this purpose because they are large enough to permit insertion of thermocouples, and still they heat fast enough to give rapid tests. Kickless cables of the interleaved type (Figure 14) were tested in a public-utility laboratory, where facilities were available for intermittent loading at 21,000 amperes. The primary purpose was to find heat balance and local temperatures under various combinations of current, cable shape and constrictions, total water flow, and ratio of water between inner and outer passages. Transient and steady-state temperatures were read for a comprehensive selection of these variables. Then intermittent load was applied, several cases having long enough period to permit temperatures to be read after each hit (probably within two degrees centigrade). From these runs data were obtained for the checks previously listed.

Tests for Exponential Rate of Change

Figure 15 shows temperature–time curves for continuous load tests, with three different rates of water flow through light-duty cables. (Similar curves were obtained for heavy-duty cables, but are not needed for this discussion.) The ordinate is really logarithm of (100 per cent temperature change), but is graduated directly in terms of per cent change.

The fact that the tests produced these straight lines gives a sound basis for the use of the exponential expression in the section, "Duty Factor: Its Use and Meaning."

The binary heating time constant is read from each curve: it is the time at which the line crosses the 50 per cent temperature value. Although further tests may refine the time constants, it should be remembered that they do not radically affect temperatures under periodic load. That is, a ten per cent difference in time constant means at most a two or three per cent change in duty factor.

The data for Figure 15 were chosen because of their connection with the inter-

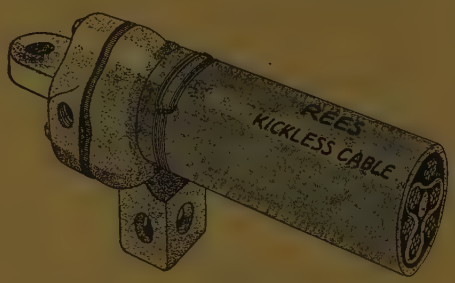


Figure 14. Light-duty Rees kickless cable upon which tests were run

The six component cables may be seen at the cut end. The three within the rubber separator are paralleled at the terminal cap, whereas the three outside the separator are paralleled in the base of the terminal

A stream of water is directed down each cable, in such volume as to equalize the temperature between inner and outer cables. Temperature difference between wire and water is minimized by small sections of copper. Continual spiraling of cables assures water cooling near all points of contact between wire and rubber

Thermocouples were placed in cables and at several points in terminals at water-outlet end of test cable. Temperatures given in other illustrations are hot-spot values

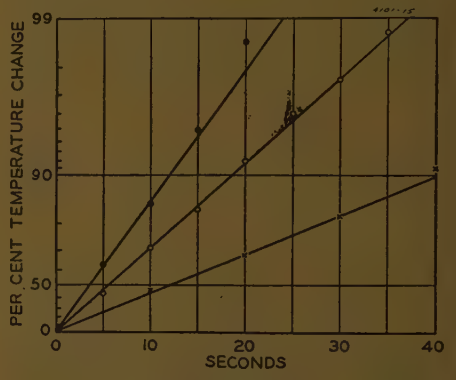


Figure 15. Heating curves for light-duty kickless cable

Each line represents a different rate of water flow through the cable

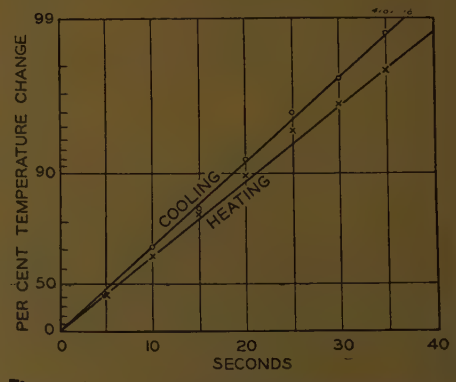


Figure 16. Heating and cooling curves for same water flow

For very accurate computations of duty factor, the difference between heating and cooling time constants may be recognized. In such a case, Figure 6 is more readily used than is Figure 7

mittent load tests, but they are representative of most results. Certain other data were not so accommodating, however, and there is some indication of curvature in the early parts of many tests. More work needs to be done on this matter, if only to determine accurate time constants for various conditions.

A point that may be important is the difference between heating time constants for heating and for cooling. In Figure 16, companion heating and cooling curves are shown. There is a noticeable difference, which seems to be consistent in all tests: cooling time constant is some 20–40 per cent less than heating This may be caused by a combination of

1. Variable losses in the cable due to increasing resistance as the wires heat.

2. The effect of difference between water temperature and copper temperature.

3. The transfer of heat along the cable as the water travels through it.

Strictly, this means that the cooling curves in Figures 8–12 should drop 20–40 per cent faster than they do.

For the present, however, this may be neglected, and the *heating* time constant may be used for both heating and cooling.

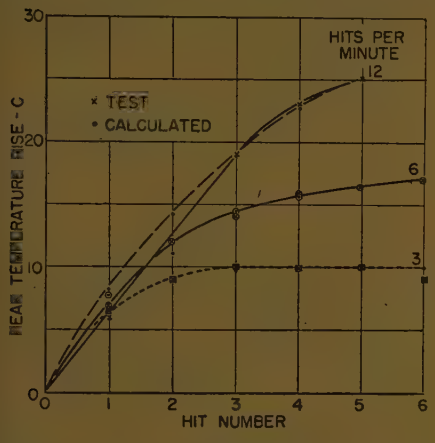


Figure 17. Test versus calculated hot-spot temperatures on light-duty Rees 267,000-circular-mil cable carrying 21,000 amperes for one-sixth second, at rates indicated on chart

Then ratings obtained for a given duty cycle will be conservative, since the calculation will not take account of the faster rate of cooling.

Peak Temperatures at Start of Periodic Heating

In one set of tests on a light-duty cable, hits of 21,000 amperes $^1/_6$-second duration were applied at the rate of 3, 6, and 12 per minute. In each case the temperature rose and fell according to the general pattern of Figure 11: and the high temperatures after each hit should follow equation 17. Test and calculated temperatures are compared in Figure 17.

Similar results were obtained when the rate of hits was changed without intermission, from three to six hits per minute.

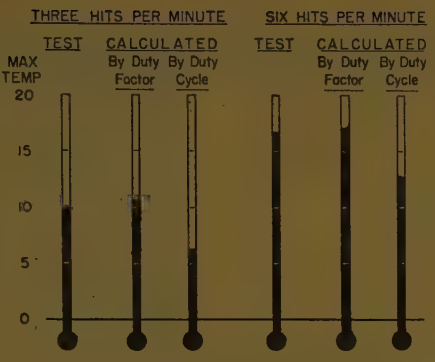


Figure 18. Comparison of test and calculated temperatures on kickless cable carrying 21,000 amperes, current "on" time one-sixth second, at welding rates noted

Dynamic-Equilibrium Temperatures

Of the three tests discussed, two were continued until successive peaks had equal values. In Table I these tests are compared with temperature rises calculated by both duty-factor and duty-cycle methods. (The theoretical temperature rise for 21,000 amperes, found by methods beyond the scope of this paper, is 760 degrees centigrade.)

Since maximum averaging time for the cable is not known, duty-cycle computations were made for one hit cycle. If the averaging time were one-sixth second longer, the temperatures in the last column would be practically doubled, emphasizing the arbitrary nature of duty cycle and averaging time.

Figure 18 shows the tabulated results graphically. The agreement between test and duty-factor results is in marked contrast to the discrepancy found in "duty cycle."

Conclusion

By means of the simple charts derived from the duty-factor method of rating under periodic load, savings can be made:

(*a*). In designing new equipment.

(*b*). In applying new loads to existing equipment.

This is in line with engineering trends to load everything to full capacity, but not beyond.

The accuracy and easy application of duty-factor method recommend it for adoption, in conjunction with the heating time constant, as a standard method of rating equipment subject to intermittent loads.

Appendix

To use this duty-factor principle on other equipment, it is necessary to know the heating constants. Little has been published on this subject. The following have been taken principally from the papers listed in reference 4:

Power Transformers[4c]

Kilovolts	Megavolt-Amperes	Time Constant (Binary) (Hours)
13	1–5	1.7
34	8–20	1.9
69	1–2.5	3.0
69	5–33	2.1
110	5	2.6
	33	2.2
138	2.5–20	3.0
	33	2.5

Motors

One horsepower—23 minutes[4b]

One-half- to five-horsepower general-purpose induction—for heating 7 minutes—for cooling 35 minutes.[4c]

Thirty horsepower, three-phase six-pole induction, 500 rpm, 440 volts, 25 cycles—30 minutes.[5]

Fifty horsepower, direct current, four pole, 1,075 rpm, 230 volts—21 minutes.[5]

Ignitron

Small water-cooled ignitron[10]—16 seconds.

References

1. Welding Handbook, 1942. Pages 282, 288.

2. Electronic Control of Resistance Welding, G. M. Chute. 1943. Pages 43, 47.

3. Rating of Industrial Electronic Tubes, O. W. Pike, Dayton Ulrey. AIEE Transactions, volume 53, 1934, December section, page 1578.

4. AIEE Standard 1A. Report on General Principles for Rating of Electronic Apparatus for Short-Time, Intermittent, or Varying Duty. Proposed Supplement to AIEE Standard 1, Introduction to AIEE Standards, September 1941.

Includes bibliography for 1930 to 1940 on principles and application to arc welding, d-c motors, transformers, fractional-horsepower motors, general-purpose induction motors, underground cable, refrigeration, and air conditioning.

(*a*). Effect of Load Factor on Operation of Power Transformers by Temperature, V. M. Montsinger. AIEE Transactions, volume 59, 1940, November section, pages 632–6.

(*b*). A Study of Short-Time Ratings and Their Application to Intermittent-Duty Cycles, R. E. Hellmund, P. H. McAuley. AIEE Transactions, volume 59, 1940, pages 1050–5.

(*c*). Rating of General-Purpose Induction Motors, P. L. Alger, T. C. Johnson. AIEE Transactions, volume 58, 1939, September section, page 455.

5. The Thermal Time Constants of Dynamo-electric Machines, A. E. Kennelly. AIEE Transactions, volume 44, 1925, page 144.

6. Heating of Railway Motors in Service and Test-Floor Runs, G. E. Luke. AIEE Transactions, volume 41, 1922, page 163.

7. Temperature Curves and the Rating of Electric Machinery, R. Goldschmidt. *Journal* of Institution of Electrical Engineers (London, England), volume 34, 1905, page 660.

8. The Heating and Cooling of Electrical Machinery, P. Grice. *Journal* of Institution of Electrical Engineers (London, England), volume 51, 1912, page 840.

9. Predetermination of Self-Cooled Oil-Immersed Transformer Temperatures, W. H. Cooney. AIEE Transactions, volume 44, 1925, page 611.

10. Analytical Treatment for Establishing Load-Cycle Rating for Ignitrons, D. E. Marshall, E. G. F. Arnott. AIEE Transactions, volume 61, 1942, August section, pages 545–8.

11. Cooling of Oil-Immersed Transformer Windings After Shutdown. (Appendix B), V. M. Montsinger. AIEE Transactions, volume 36, 1917, page 711.

Tests of 230-Kv High-Speed Reclosing Oil Circuit Breaker

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MEMBER AIEE

Synopsis: The advantages of high-speed reclosing of transmission lines in 20 cycles or less are well established for voltages up to 138 kv. Up to this date little information is available however on reclosing 230 kv lines in 20 cycles or less, because breakers have not been available for such fast operation. This paper describes a 230-kv oil circuit breaker which is suitable for three-pole or single-pole reclosing with a reclosing time of less than 20 cycles. The performance of the breaker is established by field tests for high-speed-reclosing operation as well as for interruption of charging currents.

IN the late 1920's, the increasing demand for fast-clearing circuit breakers brought forth the development of various types of modern arc-rupturing devices. Among others, there was developed the expulsion-chamber arc-rupturing device[1] which now is in satisfactory operation in thousands of breakers. The theory of expulsion action and the performance of the expulsion-chamber breakers have been reported on frequently. Test on a 115-kv system showed arcing times of approximately $1^1/_2$ cycles at short-circuit capacities ranging up to 1,650,000 kva.[1] Tests on a 230-kv system showed arcing times of approximately 2.5 cycles at ratings up to 1,125,000 kva.[2] In the middle 1930's the practice of high-speed reclosing was introduced and is now widely accepted. Interrupting capacity tests performed on a 69-kv system showed that the expulsion-chamber arc-rupturing devices gave excellent performance in conjunction with high-speed-reclosing operation.[3] Following the demand for still shorter opening time, more recently five-cycle breakers have come into wide use. On highest voltages, particularly in conjunction with switching of long lines, the use of three-cycle breakers frequently is indicated.

Finally the method of single-pole reclosing on highest voltages in recent years has attracted considerable attention. Because of the wide distribution of 230-kv systems, it was therefore decided to subject a modern five-cycle high-speed-reclosing 230-kv circuit breaker to field tests, to analyze its performance generally and in particular with regard to the possibilities of developing a three-cycle breaker. Furthermore, it was hoped that the data would enable the drawing of conclusions as to a decrease in tank diameter, oil volume, and resulting reduction in cost. Through the courtesy of the Bonneville Power Administration, the North Bonneville Substation and four generators of the Bonneville power house were made available for these tests.

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The author acknowledges the assistance of the engineering and operations departments of the Bonneville Power Administration in the performance of the tests described in this paper.

Description of Breaker, Operating Mechanism, and Test Connections

Breaker

The breaker tested consisted of a single-pole unit of a 196/230-kv circuit breaker with its high-speed-reclosing mechanism. Figure 1 shows a cross section through the breaker, which consists of six expulsion chambers arranged in series supported by the bushings. The movable blades are fastened to a vertically rotating bakelite tube. These blades move at speeds up to 28 feet per second, and therefore rapidly introduce a large break distance. The expulsion chamber resembles an explosion chamber; however, it radically differs in its action in that only moderate pressures are developed because of the provision of an expulsion port to the rear of the chamber. As previously explained,[1] the gases are accumulated to produce a more effective gas blast during the period of interruption. The oil volume within the chamber is kept small in order to prevent interference of large bodies of oil with the free escape of gases. However, an amount sufficient for multiple interruptions is provided. Figure 2 shows an expulsion chamber during the various stages of breaker opening.

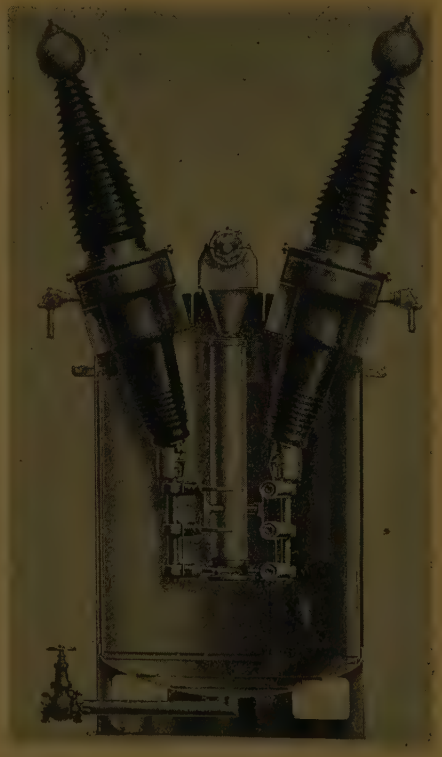
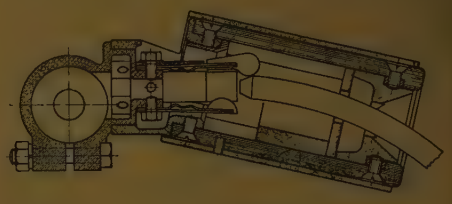


Figure 1. Cross-sectional view of the expulsion oil circuit breaker

Operating Mechanism

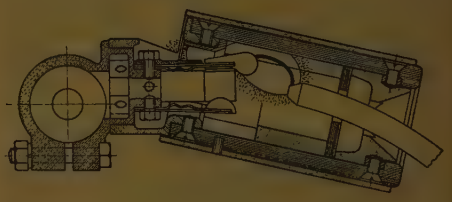
The operating mechanism is of the motor-wound spring-actuated trip-free type; its operating linkage corresponds to that of a solenoid control in which the closing coil and plunger are replaced by a prewound closing spring. When the



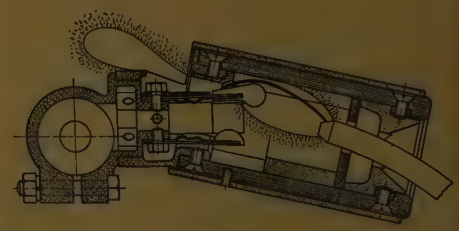
A. Contacts separate



B. Gas accumulating. No gas-blast action



C. Oil in expulsion port practically removed. No gas-blast action



D. Expulsion port cleared. High-velocity gas blast through the arc

Figure 2. Operation of expulsion chamber

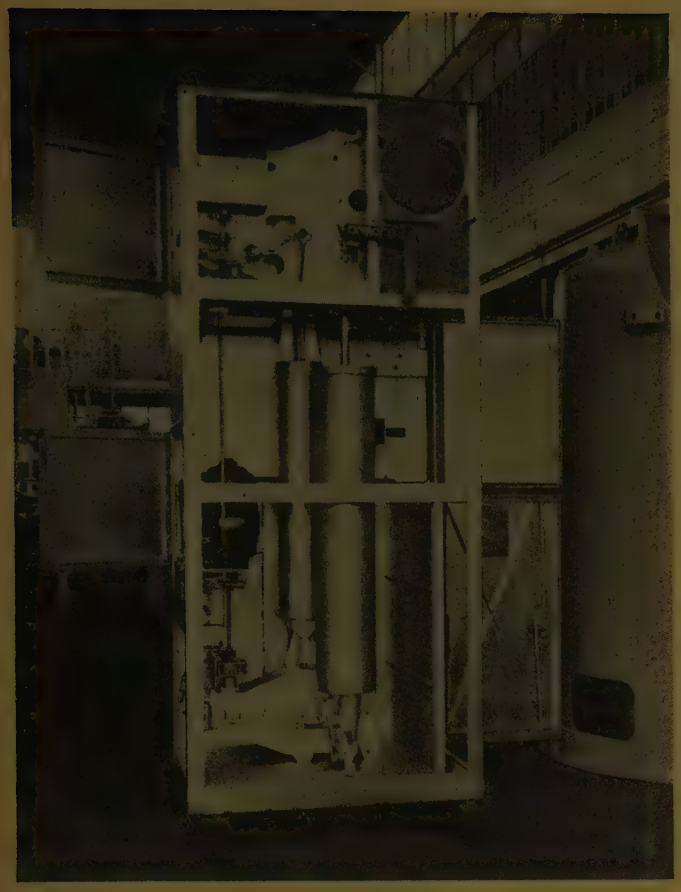


Figure 3. Operating mechanism

breaker is in the closed position, the closing spring also is wound and in case of opening of the breaker is available immediately to initiate the reclosing operation. Figure 3 shows the control applying to the breaker under test, the largest one of a line ranging from 7.5 to 230 kv. Figure 4 is a schematic diagram of the sequence of operation. The mechanism power is transmitted through a horizontal rotating shaft to the individual breaker poles and in each is transformed into the vertical breaker-shaft motion by means of a bevel link, which provides a toggle in the closed position, assuring perfect contact at all times.

During the opening or closing operation each blade provides a break of $24^3/_4$ inches, the total with six breaks amounting to $148^1/_2$ inches. During the reclosing operation the blades open to the full distance of $24^3/_4$ inches each, insuring reliable and unimpaired performance during the first opening of the series. This feature is made possible by the high speed of blade travel. In order to obtain data concerning the required blade speeds for five-cycle and possibly three-cycle breakers, the test-breaker operating mechanism was provided with various spring combinations, giving opening velocities of from 18 to 28 feet per second. When connected to a three-pole breaker, the opening time (trip coil energized to arcing tips separate) amounts to 2.5 cycles or less; however, because only one pole was tested, a reduction in initial spring pressure was made, increasing the opening time to approximately three cycles. The reason for this is that the accelerating forces upon the trip levers also are reduced, increasing the time for the blades to proceed to the stationary-contact arcing tips. This deviation from three-pole oil-circuit-breaker conditions was of no consequence since the primary purpose of the test was to obtain arcing-time data. Opening times are determined readily by factory tests.

Test Setup

System connections and power sources

1. Single-phase short-circuit-to-ground opening and reclosing tests: two or four 60,000-kva generators feeding through transformers of corresponding kilovolt-amperes over one mile of 230-kv line. Voltage-recovery rates regulated by switching on or off 133 miles of 230-kv idle line.

2. Interruptions of line-charging-current tests: The 230-kv system charging 133 miles of 230-kv line. Tests were single pole, the other two phases being opened a few cycles before, simultaneously, or a few cycles after the test oil circuit breaker.

Equipment

A seven-element magnetic oscillograph was used to record all 60-cycle data, and the oil-circuit-breaker arc-tip position and opening travel. The arc-tip indicating contact was mounted directly on the oil-circuit-breaker rotating unit, eliminating any backlash and having entirely negligible deflection due to inertia effects. Switching voltages were measured by two methods. A sphere gap connected through a capacitance divider to the bushing capacitance tap, or a cathode-ray oscillograph similarly connected and fed through a concentric line. The latter was calibrated by over-all tests between one megacycle and 60 cycles and showed negligible distortion and attenuation in this range.

Breaker Tests

Tests were conducted during December 1943 and January 1944. Table I gives data on open and high-speed open–close–open shots.

Tests 1, 2, and 3 establish an arcing time of 1.8 cycles maximum for current values of around 870 amperes. Since the breaker rating of 2,500,000 kva corresponds to a rated interrupting current of 6,300 amperes, it can be seen that the breaker is capable of interrupting short-circuit currents considerably below 25 per cent maximum rating with an average arcing time of but 1.2 cycles.

Tests 13, 14, and 26 establish a maximum arcing time of 1.4 cycles for cur-

Table I. Short-Circuit Open and High-Speed Open–Close–Open Tests

Test Number December 19, 20, 1943	Duty Cycle	Kv to Ground Before Test; Phase C	Number of 60,000-Kva Generators	133 Miles of 230-Kv Line	Opening Speed, Ft Per Sec	Rms Amperes Interrupted	Total Time to Clear	Arcing Time	De-energized Time	Kva Interrupted, Equivalent 3 Phase	Cold Gas, Generated Gallons
1	O	143	2	On	28	850	3.9	1.0		364,000	No record
2	O	144	2	On	28	870	4.7	1.8		375,000	O
3	O	144	2	On	28	870	3.5	0.7		375,000	O
13	O	134	4	Off	28	1,910	4.2	1.4		766,000	$1^1/_4$
14	O	133	4	Off	28	1,860	4.1	1.2		742,000	$^1/_2$
26	OCO 1st opening	136	4	Off	23	1,900	4.5	1.4		775,000	$1^1/_2$
	OCO 2d opening		4	Off	23	1,970		0.5	12.6	805,000	

All times are in cycles at 60 per second. O—Open. OCO—Open–close–open.

Table II. Single-Phase Line-Charging-Current Interruption Tests

Line Kilovolts to Ground 127 to 130 Rms Amperes from 70 to 105

Test Number and Date	Opening Speed, Ft Per Sec	Resistors Across Expulsion Chambers	Backup OCB Phase A and B Open Before or After Phase C of Test OCB, Cycles	Cycles of Arc	Number of Restrikes	Ratio of Phase C Maximum Voltage to Normal Crest—Does Not Exceed: By Cathode-Ray Oscillograph	By Spark Gap
December 19, 1943							
15	28	No	0.2 before	1.0	1		
16	28	No	8.8 after	0.9	1		
17	28	No	10.1 before	1.2	1		
18	28	No	11.3 before	0.14	0		
19	28	No	11.2 before	1.1	1		
20	24	No	0.7 before	1.5	2		
21	24	No	1.3 before	1.2	1		
23	24	No	3.4 before	1.1	1		
24	24	No	1.7 before	1.2	1		
25	24	No	0.7 before	1.0	1		
January 9, 1944						Bus Side	Line Side
1	28	Yes	0.0 before	1.9*	0	1.2	1.36
2	28	Yes	1.4 before	2.1*	2	1.2	1.36
3	28	Yes	0.8 before	2.6*	0	1.2	1.36
4	21.5	Yes	0.5 before	3.0*	0	1.2	1.36
5	21.5	Yes	0.5 before	2.9*	0	1.2	1.36
6	21.5	Yes	1.1 before	2.2*	0	1.2	1.36
7	18	Yes	3.7 before	2.8*	0	1.2	1.36
9	18	Yes	3.4 before	3.1*	1	1.2	1.36
10	18	Yes	3.6 before	3.8*	2	1.2	1.36
11	28	No	7.0 after	1.14	1	1.1–1.2	1.36
12	28	No	7.1 after	0.8	1	1.1–1.2	1.36
13	18	No	5.3 after	2.0	3	1.1–1.2	1.36
14	18	No	5.0 after	1.1	1	1.1–1.2	1.36
15	18	No	5.6 after	1.4	2	1.1–1.2	1.36
						Line Side	
16	18	No	4.6 after	1.2	3	1.55	Discontinued

All times are in cycles at 60 per second. * Arc tips part to final clearing. OCB—Oil circuit breaker.

rents above 25 per cent of full breaker rating for operating speeds as low as 23 feet per second. Since no lines were connected during these tests, the rates of rise of recovery voltage were high, and the tests represent a condition more severe than can be expected during normal operating conditions. The maximum current interrupted is considerably below the rated interrupting current; however, previous published tests on a 115-kv system up to 10,000 amperes have established the fact that the arcing time decreases with increasing current. This decrease in arcing time is due to the fact that higher currents generate gases at an increased rate, producing a more effective gas-blast action. Important loads precluded the possibility of tests including the entire system. However, since the performance of the expulsion chambers at higher currents is so well established, it may be assumed that such tests could not but conform.

Test 26 represented by oscillogram, Figure 5, gives performance under high-speed-reclosing conditions. The reclosing time amounted to 17 cycles, the de-energized time to 13 cycles. The first interruption occurred with an arcing time of 1.4 cycles, the second one with an arcing time of only one-half cycle.

Much effort has been spent in recent years to produce rupturing devices for high-speed-reclosing oil circuit breakers with the purpose of reducing the arcing time of the second interruption to that of the first one. In considering the expulsion chambers, it could be assumed that a definite amount of time is required for the chamber to recover its interrupting ability. However, since these chambers are operating on the gas-blast principle and require but a small amount of oil for each interruption, and since a liberal amount of oil is present in the expulsion chamber, it can be anticipated that the chamber requires no recovery time at all. This assumption is verified by the short arcing time of only one-half cycle during the second opening.

During all the aforementioned tests, the vent pipe was completely closed off by a deflated paper bag. The amount of inflation of this bag represents a simple means of approximating the volume of gases produced during each interruption.

Figure 4. Operating-mechanism linkage

A. Breaker closed

Closing spring and opening spring charged, ready for trip, immediate reclose, and second trip cycle

B. First opening

Trip-coil plunger trips trip toggle which starts resetting as soon as pin *A* of main linkage has cleared prop. Opening spring opens breaker and brings pin *A* into vicinity of closing lever which is released by means of closing-coil plunger

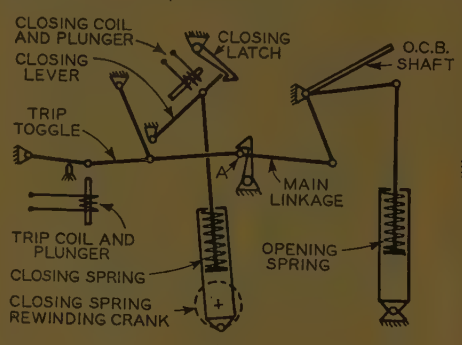
C. Closing

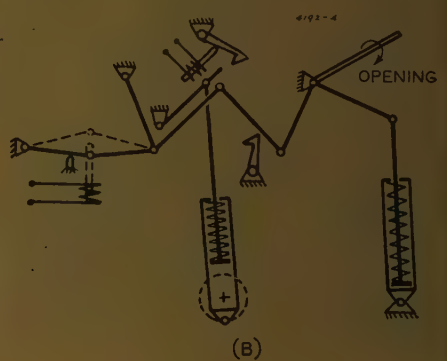
Upon release of closing lever (Figure 4B), breaker is closed and prop engages pin *A*. Trip coil is again energized and plunger breaks opening link which resets as soon as pin *A* has cleared prop and pawl on closing lever

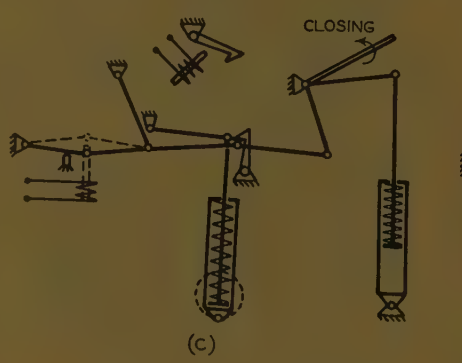
D. Second opening

Breaker opens as in Figure 4B, and motor limit switch energized turning crank one revolution, resetting closing lever on closing latch and charging spring

When breaker is closed and remains closed, motor again turns crank one revolution, bringing control to original position, Figure 4A, ready for immediate-reclosing cycle







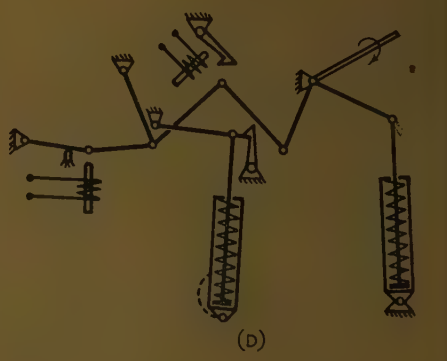




Figure 5. Magnetic oscillogram of reclosing test 86, December 20, 1943

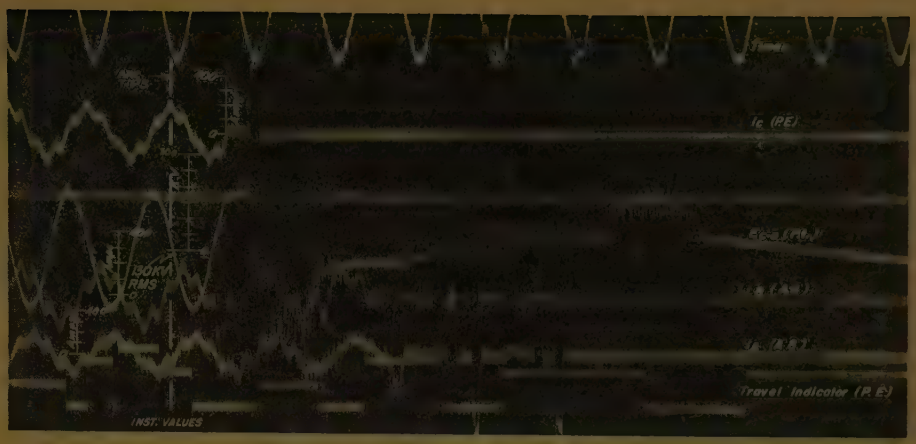


Figure 6. Magnetic oscillogram of charging-current interruption, test 15, December 19, 1943

As can be seen from the last column of Table I, the maximum amount was $1^1/_2$ gallons. The inflation of this paper bag was the only visible evidence of the interruption; no disturbance was noticeable during any of these tests.

Charging-Current-Interruption Tests

During these tests, one phase of a three-phase line charged by the substation bus was opened by the test breaker; the other two phases were opened a few cycles before, simultaneously, or a few cycles after by a station breaker.

It has been recognized for a long time that the interruption of charging currents of long transmission lines represents the severest duty that can be imposed upon a circuit breaker.[4] The theory of restrikes and the subsequent building up of high overvoltages has been reported upon in detail[5] and requires no further discussion here. Recent investigations show that breakers equipped with most modern arc-rupturing devices can produce overvoltages in excess of $4^1/_2$ times normal voltage, because of multiple restrikes, and that these voltages can be reduced to harmless values of twice normal voltage by the provision of resistances across the interrupting devices.[6]

Accordingly, parallel resistances of 500 ohms were built for installation across each of the six expulsion chambers. Tests were performed with and without resistances; in addition, the opening speed was varied. Table II gives the results of the charging-current-interruption tests.

Of interest is the very short arcing time without use of resistances and its increase with decrease in opening speed. At 28 feet per second speed, without resistances the number of restrikes was limited to one, at 24 feet per second two restrikes occur in one case out of 5, and at 18 feet per second the maximum number of restrikes amounts to 3. Resistances eliminate the restrikes except for small suppressed restrikes, but result in an increase in effective arcing time to values as high as 3.3 cycles against a maximum time without resistances of two cycles. Voltage measurements by cathode-ray oscillograph and spark gap show a maximum possible value of 1.36 times normal voltage. No voltage records were taken during the December 19 test; however, since the highest voltages are likely to occur at the lowest operating speeds and corresponding maximum number of restrikes, it is felt that the omission is not important. The behavior of the expulsion chamber in interrupting charging currents in such short times without the use of resistances or other auxiliary devices seems to justify a detailed study of the causes of this unusual performance.

Considering the mechanism of generation of overvoltages by the restrikes, it may be proved that to prevent such overvoltages the reignition voltage should be less than the normal line-to-ground voltage. Since the voltage builds up to the normal value within one-quarter cycle, the current-zero period should be one-quarter cycle or less. Only those restrikes in which the current-zero interval exceeds one-quarter cycle will cause overvoltages. Numerous successive delayed restrikes with current-zero intervals of less than one-quarter cycle are therefore harmless, as is evidenced by operating experience on numerous plain-break breakers known not to produce harmful overvoltages.

An arc-rupturing device therefore should have a very low dielectric-recovery rate, permitting restrikes within less than one-quarter cycle after current interruption until such time that the device is capable of withstanding full voltage. Since in the gas accumulation period, the expulsion chamber constitutes a plain-break switch and since in the low-current range this period amounts to approximately one cycle, an early and harmless restrike can be expected. After this period the gas-blast action starts and causes final interruption of the arc at the next current zero.

Figure 6 shows an oscillogram of the interruption corresponding to test 15,

December 19, at a speed of 28 feet per second. A restrike occurs in less than one-quarter cycle after the first current zero to be followed by final interruption. Similar performance with not more than one restrike was obtained on all interruptions with a speed of 28 feet per second. Opening speeds of 24 feet per second as shown from test 20, December 19, can produce two restrikes of which the second one can be preceded by a zero-current interval in excess of one-quarter cycle. However, even if the delay amounts to the critical one-half cycle period, the theoretical voltage cannot exceed three times normal line voltage. Actual voltage measurements during operations with opening speeds as low as 18 feet per second and a maximum of three restrikes did not indicate any appreciable overvoltage.

The expulsion-chamber breaker, when operated with speeds above 24 feet per second, interrupts charging currents without producing overvoltages. Operation at lower speeds, although theoretically capable of producing overvoltages of three times normal, does not produce harmful values.

Conclusions

1. The expulsion-chamber rupturing devices are capable of interrupting short circuits on a 230-kv system in less than 1.5 cycles, making expulsion oil circuit breakers having an interrupting time of considerably less than five cycles a possibility.

2. Successive interruptions during high-speed-reclosing operations with only 13 cycles de-energized time are handled without difficulty by the expulsion chamber.

3. The expulsion chambers are suited ideally for the interruption of charging currents. The arcing times are small, and no harmful overvoltages are produced. Parallel resistances can be applied to expulsion chambers; however, they produce no improvement in performance.

4. The motor-wound spring-actuated operating mechanism produces reclosing speeds not heretofore reached and permits full opening of the breaker during all reclosing operations.

References

1. The Expulsion Oil Circuit Breaker, A. C. Schwager. AIEE Transactions, volume 53, 1934, July section, pages 1108–15; discussion, volume 54, 1935, February section, pages 212–16.

2. Expulsion-Breaker Field Tests, A. C. Schwager, V. A. Treat. *Electrical World*, February 27, 1937.

3. The Ultrahigh-Speed-Reclosing Expulsion Oil Circuit Breaker, A. C. Schwager. AIEE Transactions, volume 56, 1937, August section, pages 968–70.

4. High-Voltage Oil Circuit Breakers (book), Wilkins, Crellin. Page 277.

5. Effect of Restriking on Recovery Voltage, C. Concordia, W. F. Skeats. AIEE Transactions, volume 58, 1939, August section, pages 371–5.

6. Switching Overvoltages Eliminated in High-Voltage Oil Circuit Breakers, L. F. Hunt, E. W. Boehne, H. A. Peterson. AIEE Transactions, volume 62, 1943, February section, pages 98–106.

Operation of Low-Pressure Gas-Filled Cable

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SINCE 1938 low-pressure gas-filled cable has been the source of considerable discussion before meetings of the Institute and other technical associations. Papers presented by Shanklin in 1939[1] and 1942[2] and an unpublished paper by Del Mar and Gambitta in 1943 discussed the theoretical aspects of a cable utilizing gas as pressure medium and presented extensive laboratory test data on the expected life of this type of cable. From these data it was indicated that if low-pressure gas-filled cable is operated at an average stress of less than 75 volts per mil and at a minimum gas pressure of ten pounds per square inch satisfactory operation may be expected.

Theories and laboratory test data are not always sufficient to establish a fact and they should be substantiated by actual field data whenever possible to prove the theory. It is intended in this paper to present factual data on the actual operation of low-pressure gas-filled cable of various types on the system of the Consolidated Edison Company of New York, Inc. Operating experience data in the report are divided into (1) sheath experience and (2) electrical quality of the insulation of cable removed from service.

History of Installation

In view of the satisfactory operation of oil-filled feeders installed in 1927 and 1929 where for the first time complete sheath supervision was obtained, it seemed apparent that if supervision could be maintained over the lead sheaths of solid-type cables a large percentage of electrical failures could be eliminated. The low-pressure gas-filled cable described by Shanklin in his 1939[1] paper appeared to offer a satisfactory solution to this problem of sheath supervision, for feeders operating at medium voltages, such as 15,000 volts and 27,000 volts and in addition appeared to provide greater stability against ionization. With this type of cable, positive pressure could be maintained within the cable thereby reducing the possibility of water entering the cable in the event of sheath or joint defects and providing a means of indicating when sheath or joint defects occurred.

In order to obtain information on actual field operating conditions there was installed on the Consolidated Edison system during the year 1938 approximately 20,000 feet of three-conductor 800,000-circular-mil 15,000-volt low-pressure gas-filled cable. The operation of the initial 1938 installation was followed closely in the field and the results obtained appeared to check factory laboratory results. To obtain additional information on this low-pressure gas-filled cable at

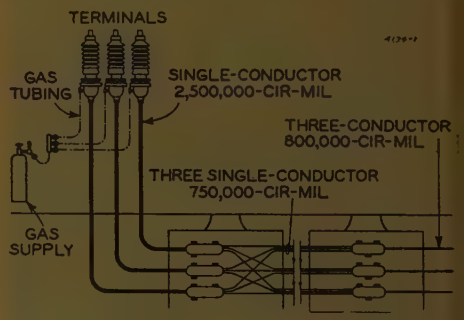


Figure 1. Typical installation of gas-filled cable at generating stations

Connection of single-conductor to three-conductor cable

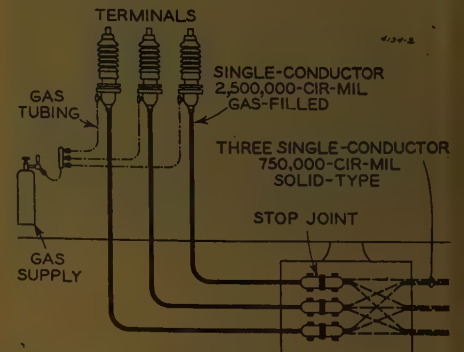


Figure 2. Typical installation of gas-filled cable at generating stations

Connection of gas-filled to solid-type cable

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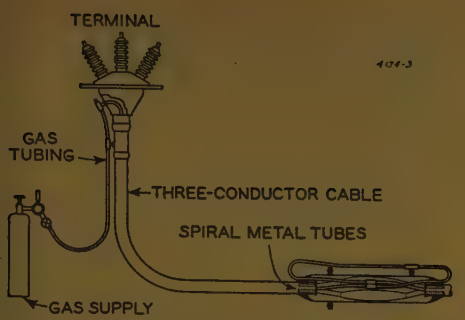


Figure 3. Connection of solid-wall tubing to joints and terminal

higher operating voltages, there was installed during 1939 approximately 8,000 feet of three-conductor 500,000 circular-mil 27,000-volt cable.

The construction program for the year 1940 involved extensive feeder rearrangements around several generating stations and the installation of additional 27,000-volt feeders between generating stations. The information which had been obtained from the 15,000-volt cable installed during 1938 and the 27,000-volt cable installed during 1939 indicated that the low-pressure gas-filled cable would be a desirable type of cable for the new cables required at he generating stations as it was desired to obtain cables where supervision could be maintained over the lead sheaths. The cables required for station lengths at the generating stations were of the single-conductor type so as to obtain phase isolation in accordance with our standard practice in generating stations. The cable manufacturers were approached at this time as to the possibility of obtaining single-conductor low-pressure gas-filled cables as all of the past experience had been obtained on three-conductor gas-filled cable. Agreement was reached on the type of single-conductor cable to be used for low-pressure gas-filled cables and our requirements for the feeder rearrangements at the generating stations were purchased as low-pressure gas-filled cable.

All of the low-pressure gas-filled cable installed on the system between 1938 and 1940 was in relatively short runs, the maximum length being approximately two miles. Runs of this length presented no serious difficulty in maintaining proper gas pressure along the entire run of cable with one point of supply. In 1941 it was decided to install a feeder from one of our generating stations to a substation approximately six miles away. Three-conductor 800,000-circular-mil 15,000-volt gas-filled cable was purchased and installed for this feeder and a single point of supply was provided for maintenance of gas pressure.



Figure 4. Provision of gas space in single-conductor cable by use of fluted lead sheath or button-embossed spacer tape

Table I. Cable Operating Under Nitrogen Pressure (13 Pounds Per Square Inch) on System of Consolidated Edison Company of New York, Inc., and Affiliated Companies

Cable Feet as of June 1, 1944

Number of Conductors	Size, Thousands of Circular Mils	Rated Voltage 27 Kv	Rated Voltage 15 Kv	Total
1	750		1,100	1,100
1	1,000	9,650	30,000	39,650
1	2,500	1,500	7,400	8,900
3	500	38,625		38,625
3	800		49,800	49,800
		49,775	88,300	138,075

Types of Cable and Cable Arrangements

As of June 1, 1944, the total amount of low-pressure gas-filled cable installed on the system is as shown in Table I.

As indicated in Table I several sizes and types of cables have been used. In general it has been necessary to use several different sizes of single-conductor cable at generating stations in order to make optimum use of existing ducts, to obtain space for terminations, and to utilize existing solid-type cables. No difficulty has been experienced in using gas-filled cable for paralleling lengths of cable, connecting a large single-conductor cable to several smaller cables, or connecting to solid cables. Typical installations of gas-filled cable at generating stations and connections to solid cable are shown on Figures 1 and 2.

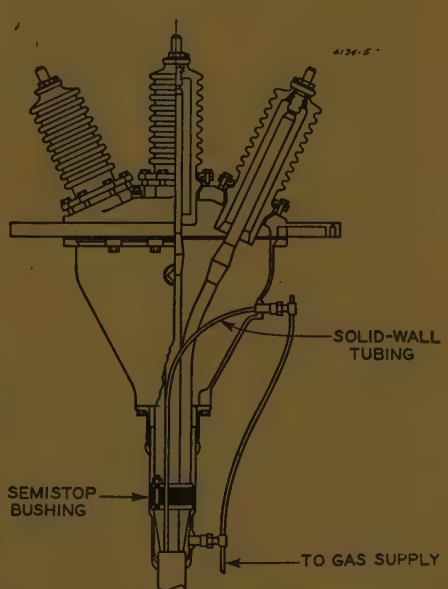


Figure 5. Three-conductor compound-filled terminal for use on gas-filled cable

Method of Installation

The three-conductor cable which was installed during 1938 was manufactured with three spiral tubes in the interstices. Due to oil drainage from the paper insulation, oil slugs were formed in the spiral tubes causing partial blockage of the channel spaces and preventing free flow of the nitrogen gas along the entire length of the feeder from the one point source of supply. On all subsequent three-conductor cable which has been purchased, a solid-wall copper tube for one of the three gas channels has been specified. This solid-wall tube is connected into each joint in a manner which prevents the entrance of cable compound into the tube and supplies a point of feed to the cable as shown by Figure 3. This method provides a gas supply manifold along the entire route of a feeder since the source of gas supply, namely, a cylinder of nitrogen, is connected to a terminal or joint where the solid-wall tube ends. This type of construction provides continuous supply of gas to every individual joint and tends to maintain uniform pressure along the line.

In single-conductor gas-filled cable, channels for the flow of gas were obtained by two methods: (1) the application of a button-embossed copper tape between the cable core and the lead sheath, and (2) by the use of fluted lead sheath. Figure 4 shows the two types of construction of single-conductor cable.

At the time of the installation of the original gas-filled cables at the generating stations, gas-filled terminals were not available. Development work had not progressed to the point where manufacturers were willing to guarantee clearances within the terminals with only nitrogen gas as a filling medium. In order to carry the gas-filled cable into

Table II. Classification of Leaks

	Total Number Installed	Number of Leaks
Terminals	350	30
Relays	24	8
Fittings		19
Normal joints	581	8
Stop joints	46	0
Gas-supply equipment	24	30
Cable sections	759	4
Cable adjacent to joint		46
Not located		18
Total leaks		163

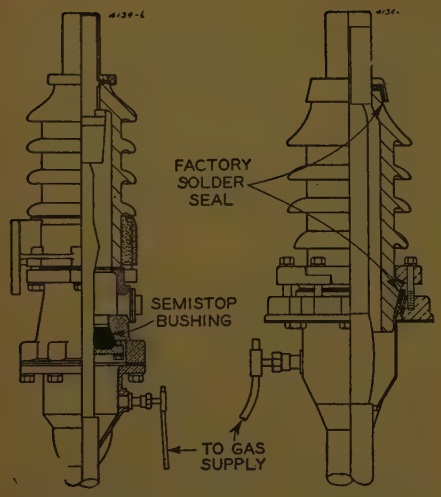


Figure 6 (left). Single-conductor compound-filled terminal for use on gas-filled cable

Figure 7 (right). Single-conductor gas-filled solder-seal terminal for use on gas-filled cable

terminals a standard-type terminal was used with a semistop feature incorporated in the wiping sleeve of the terminal. The semistop feature was installed to prevent the filling compound within the terminal from flowing into the gas-filled cable. This semistop feature did not in all cases prevent the gas from entering the terminal after it had been completely installed. All of the original terminals which were used were of the normal gasketed type. Figures 5 and 6 show the terminals used for three-conductor and single-conductor cable with the semistop features which were incorporated in the wiping sleeves. Considerable difficulty was experienced with leaks at the gaskets in these terminals. Terminals which were initially tight developed leaks after the gaskets had been subjected to working when the cables were loaded. In order to eliminate this source of trouble development work was continued by the manufacturers on gas-filled terminals with the final result that there became available gas-filled solder-seal-type terminals which eliminated all gaskets. On recent installations of single-conductor gas-filled cable at stations, this type of terminal has been used. Figure 7 shows such a gas-filled terminal.

Types of Installations

There are now in service on the Consolidated Edison system 21 feeders which are composed in part of gas-filled cable. The feeder which includes the longest continuous run of gas-filled cable is one which operates at 13.2 kv and is composed of three-conductor 800,000-circular-mil cable extending a distance of approximately 33,000 feet from a point near the Sherman Creek generating station to the Columbus Avenue substation of The Yonkers Electric Light and Power Company in Yonkers, N. Y.

Another feeder 7,500 feet in route length is composed of three legs of 27-kv cable from the East River generating station to a submarine crossing across the East River. There are also two three-conductor 27-kv feeders approximately 5,600 feet in length which extend from the Hell Gate generating station to the Queens district. These two feeders are installed through a tunnel underneath the East River and have vertical riser lengths of approximately 250 feet at each end. Most of the remaining 17 feeders have relatively short sections of gas-filled cable adjacent to the stations which is predominantly single-conductor cable. Because of this use as station legs there is a considerable number of terminals in proportion to the length of cable involved. Figure 8 shows a typical profile of one of the feeders and Figure 9 shows the profile of a feeder through a tunnel.

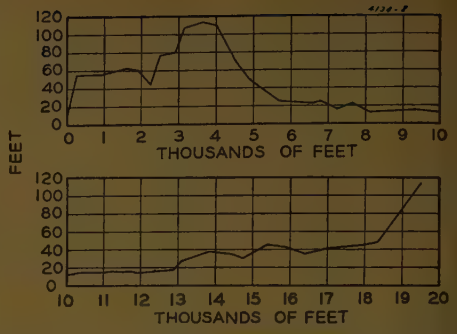


Figure 8. Portion of profile of three-conductor 800,000-circular-mil 13.2-kv gas-filled feeder

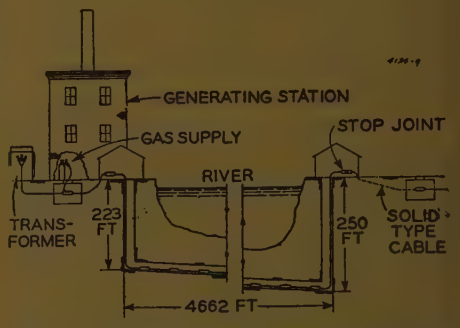


Figure 9. Profile of three-conductor 500,000-circular-mil 27-kv gas-filled feeder installed in tunnel under East River

Operation

Mechanical Trouble

Gas leaks have developed from time to time in the cable joints and terminals. In general, with one outstanding exception, it can be said that leaks have developed shortly after the cable has been placed in operation, as defective wipes, tubing connections, and terminal gaskets or seals are most apt to begin leaking as soon as cable movement occurs due to loading.

Very fast leaks due to bull-pointing the sheaths, and very minute leaks have been experienced. In all cases it has been possible to maintain gas pressure until the leak has been found and repaired.

The approximate location of leaks is normally determined by the pressure-drop method; that is, a fixed pressure is maintained at both ends of a known run of cable and pressure is measured at the intervening joints. The point of lowest pressure is the approximate location of the leak. The application of liquid soap to all exposed parts of the system at this location will then reveal the actual point of leakage if it is outside of the conduit run. In order to reduce the time required to locate a leak on a long run of three-conductor cable, gas-flow direction indicators are permanently installed at specified intervals so that the direction of gas flow past these points can be determined.

The indicator consists of two glass jars, each filled half-way with a light cable oil and sealed gas tight with a metal cover

Table III. Operating Record of Solid-Type Cable

Three-Conductor and Single-Conductor, 15-Kv to 33-Kv, Two Years Old in Year Shown

	Cable					Joints		
		Failures		Removals			Failures	
Year	Mile Years	Number	Rate Per 100 Mile Years	Number	Rate Per 100 Mile Years	Joint Years	Number	Rate Per 1,000 Joint Years
1940	170	13	7.7	1	0.6	4,942	9	1.8
1941	160	11	6.9	0	0	4,428	10	2.3
1942	253	9	3.6	1	0.4	7,522	7	0.9

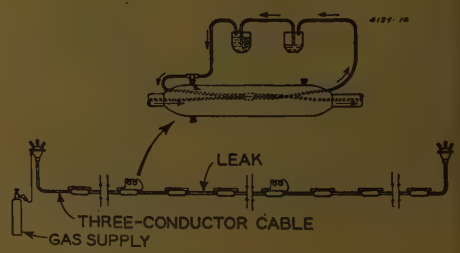


Figure 10. Installation of gas-flow direction indicators on three-conductor cable for location of gas leaks

Table IV. Operating Record of Solid-Type and Gas-Filled Cable Combined

Three-Conductor and Single-Conductor, 15-Kv to 33-Kv, Two Years Old in Year Shown (Assuming Failures Had Resulted From One-Half the Gas Leaks)

Year	Cable					Joints		
		Failures		Removals			Failures	
	Mile-Years	Number	Rate Per 100 Mile-Years	Number	Rate Per 100 Mile-Years	Joint-Years	Number	Rate Per 1,000 Joint-Years
1941	161	12	7.5	0	0.0	4,454	11	2.5
1942	263	11	4.2	1	0.4	7,798	9	1.2

and gasket. The two jars are connected in series and are inserted in the solid wall gas tube of the cable at a joint location. Under normal conditions when there is no flow of gas in the cable the oil in the two glass jars remains at the same level. When there is a flow of gas due to a leak gas will flow from the supply tank into the indicator forcing all of the oil into the glass in the direction of the leak. The direction of gas flow is also indicated by bubbles rising to the surface of the oil in the jar in the direction of the leak. The indicator and a typical installation are shown in Figure 10. With this method a leak can very readily be located between two indicator locations and the pressure-drop and liquid-soap method used for definite location within the section.

On the installations of parallel lengths of single-conductor cable at generating stations the gas supply from a common tank to individual cables has been carried through valves. When a leak is indicated by loss of gas from the tank the particular length of cable which is leaking can be determined by operation of the valves.

In very serious cases of sheath rupture it has been possible to maintain gas pressure and continue operation of a feeder. The 13.2-kv Sherman Creek feeder previously referred to as being the longest run of gas-filled cable was placed in service in October 1941. Prior to 1943 due to system operating conditions it carried little or no load. During 1943 it became necessary to load the cable to its normal load for approximately 20 hours each day with very light loads for the remaining 4 hours. With this load cycle, movement of cable in the manhole occurred and due to cable arrangement in the manhole the sheath was buckled and cracked.

Subsequent to finding the first sheath breaks a survey was made of the manholes and it was found that in 74 per cent of the manholes inspected the cable sheath was buckled adjacent to the joint wipe. Breaks have occurred in these buckles in approximately one-fourth of the manholes. The same sheath failures would have occurred in solid cable of the same conductor size installed under the same conditions and would without doubt have been responsible for a number of electrical failures.

The leaks have been found by the methods previously described and repairs have been made by patching the sheath before any known water was admitted.

A summary of all leaks which have occurred on all of the gas-filled cable installed on the system is shown in Table II. The types of leaks which have developed do not indicate that any particular type of cable is more or less susceptible to trouble than any other type.

Of the four leaks noted in the table as being in cable sections, one was caused by bull-pointing and was repaired in place without removing the cable; another necessitated cutting the cable at the point of leakage and replacing a short section of cable, while the remaining two leaks which were caused by foreign matter in the ducts required the replacement of the sections.

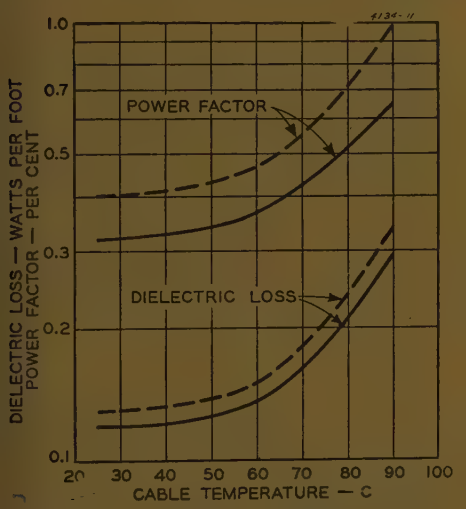


Figure 11. Power factor and dielectric loss at 27 kv of three-conductor 500,000-circular-mil gas-filled cable before being placed in service and after one year of operation as generating-station tie feeder

Ten pounds per square inch nitrogen pressure, 225-mil insulation

Solid curves—Before placing in service

Dashed curves—After one year in service

Electrical Trouble

Only one electrical trouble has occurred on the gas-filled cable. This fault was a test failure on a single-conductor 15,000-volt cable which had been severely damaged during installation. Examination of the fault indicated that approximately half of the paper tapes had been broken and torn by a severe twist and failure occurred at the point of broken tapes when a proof test was applied prior to placing in service.

No service failures have occurred in any of the 138,000 feet of gas-filled cable installed on the system.

That electrical failures could have been expected on this amount of cable, had it been of the solid type instead of gas filled, is indicated by the data contained in Table III. This table gives the failure rates in 1940, 1941, and 1942 of solid-type cable of comparable voltage rating which was two years old in those years.

For purposes of comparison Table IV shows for 1941 and 1942 the estimated failure rates which would have obtained if gas-filled cable had not been used and one half of the gas leaks found in cable sections and joints had resulted in failures during this two-year period of operation. That none of the gas leaks did cause failures may be attributed to the advantage of sheath supervision whereby sheath breaks are quickly discovered.

Electrical Characteristics

Manufacturers' laboratory test data have indicated that ionization will not take place in gas-filled cable if it is operated below an average stress of 75 volts per mil and with a minimum gas pressure of 10 pounds per square inch. All gas-filled cable operating on the Consolidated Edison Company system is supplied normally with gas at 13 pounds per square inch pressure and in case of a leak the pressure may drop to a minimum of 8 pounds per square inch for a short period of time.

The average stress for the cables ranges

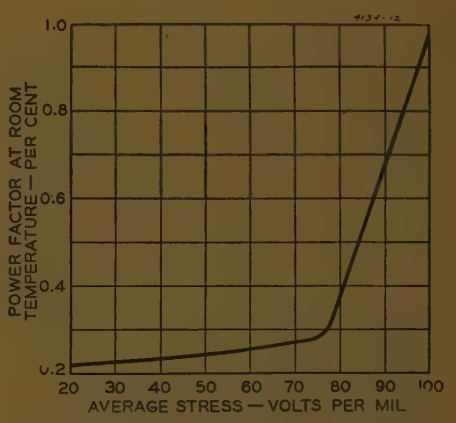


Figure 12. Ionization at room temperature of three-conductor 500,000-circular-mil 27-kv gas-filled cable after one year of operation as generating-station tie feeder

Ten pounds per square inch nitrogen pressure, 225-mil insulation

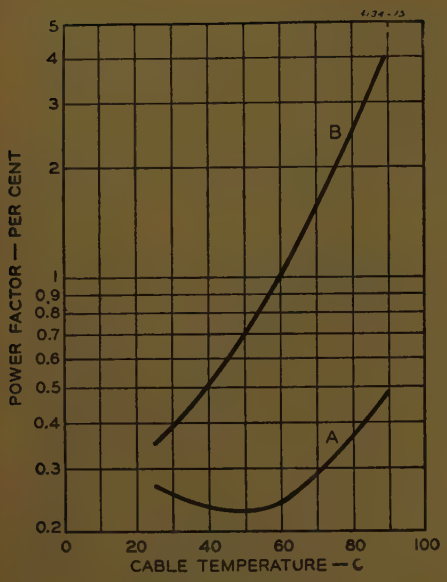


Figure 13. Power factor at 15 kv of three-conductor 800,000-circular-mil gas-filled cable before being placed in service and after approximately five years on the system

16 pounds per square inch nitrogen pressure, 150-mil insulation
A—Before placing in service
B—After 15 months loaded, 17 months dead, and 27 months loaded

between 39 volts per mil as a minimum and 68 volts per mil as a maximum. In order to determine if any ionization is taking place two sections of cable have been withdrawn for measurement of power factor and losses and for a visual examination of the papers.

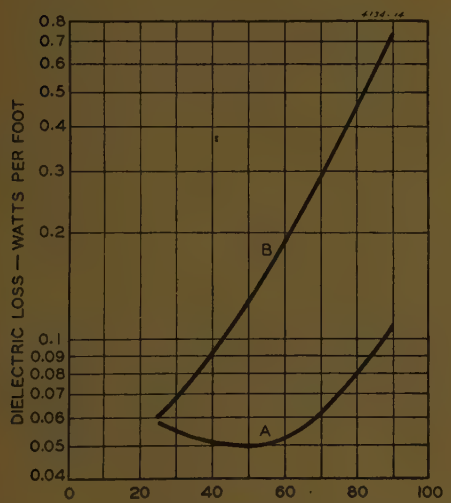


Figure 14. Dielectric loss at 15 kv of three-conductor 800,000-circular-mil gas-filled cable before being placed in service and after approximately five years on the system

16 pounds per square inch nitrogen pressure, 150-mil insulation
A—Before placing in service
B—After 15 months loaded, 17 months dead, and 27 months loaded

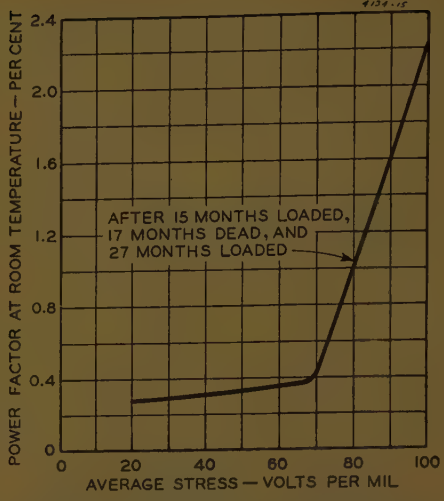


Figure 15. Ionization at room temperature of three-conductor 800,000-circular-mil 15-kv gas-filled cable after approximately five years on the system

After 15 months loaded, 17 months dead, and 27 months loaded

A 320-foot length of three-conductor 500,000-circular-mil 27,000-volt cable having 225 mils of paper insulation was removed from service after one year of operation at 27,000 volts. This cable had been operated as part of a tie feeder between generating stations.

Power-factor and dielectric-loss measurements were made on the section of cable and are shown in Figure 11 with the original factory measurements for comparison.

The power factor measured at room temperature at various volts-per-mil stress is shown in Figure 12. The ionization at 85 volts per mil after one year of operation is in agreement with data obtained from approximately 200 days of laboratory load cycle tests.

Visual examination and power-factor measurements of the paper tapes from the cable did not reveal any indication of deterioration. The five paper tapes nearest the conductor contained a few streaks, about the original color of the paper, which were more pronounced in the paper nearest the conductor. It was generally agreed that the streaks were not wax but possibly some form of oxidation products.

A 275-foot length of three-conductor 800,000-circular-mil 15,000-volt cable having 150 mils of paper insulation was removed from service after having been on the system for approximately five years. During this period the cable was operated for 15 months at an average stress of 44 volts per mil; was out of service for 17 months; and then was operated again for 27 months at an average stress of 53 volts per mil.

Power-factor and dielectric-loss measurements were made on the section of cable and are shown in Figures 13 and 14, respectively.

The power factor measured at room temperature at various volts per mil stress is shown in Figure 15.

Visual examination and power-factor measurements of the paper tapes from the cable did not reveal any indications of deterioration.

Examination of the dielectric-power-factor values shown in Figures 11 and 13 shows a greater increase in power factor for the 15,000-volt cable which had been on the system for approximately five years. With the exception of a longer period of operation the only different operating condition between the 15,000-volt cable and the 27,000-volt cable was in the amount of nitrogen which was injected into the cable after installation. Some leaks occurred on the 15,000-volt line necessitating feeding nitrogen into the line to maintain proper gas pressure thereby subjecting the insulation to exposure to a greater volume of nitrogen containing some traces of oxygen. It is possible that the insulation may absorb the oxygen from the nitrogen and an oxidation action take place resulting in an increase in power factor. This action may be the cause of the higher power-factor values for the 15,000-volt cable.

Conclusions

1. Operating experience to date has shown that electrical failures commonly experienced on new installations of solid-type cable have been eliminated by the use of gas-filled cable.

2. Breaks in cable sheaths and joint wipes and defects in terminals are ascertained immediately and remedial measures can be taken before moisture is admitted which would result in eventual electrical failure.

3. Limited data on cable in operation indicate that gas-filled cable is stable and confirms laboratory test data.

4. No serious operating difficulties due to migration of compound have been experienced with the use of gas-filled cable where there are marked differences in elevation.

References

1. Low-Gas-Pressure Cable, G. B. Shanklin. AIEE Transactions, volume 58, 1939, July section, pages 307–15.

2. Low-, Medium-, and High-Pressure Gas-Filled Cable, G. B. Shanklin. AIEE Transactions, volume 61, 1942, pages 719–26.

3. Gas-Filled Cable Aids System Supervision, E. R. Thomas. *Electrical World*, September 6, 1941.

4. Low-Pressure Gas-Filled Cables Go on 34.5-Kv Duty, S. W. Winje, M. F. Schonefeld. *Electrical World*, October 2, 1943.

5. Reeves Avenue–Taussig Substation 38-Kv Gas-Filled Cable, Norfolk, Virginia, W. F. Smith. *Electrical World*, November 13, 1943.

Radio-Noise Elimination in Military Aircraft

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RADIO noise may be defined as radio-frequency energy which interferes with the reception of desired signals. This interference can be the result of:

1. Atmospheric electrical discharges which result in radiated radio frequency energy.
2. Periodic corona discharges from the airplane which cause intense electric induction fields close to the receiving antennas.
3. Receiver internal noise due to thermal agitation in the receiver input resistance and shot effect in the low-level radio-frequency stages.[1]
4. Electrical transients which result from the operation of electrical equipment.

Methods of reducing the first two types of disturbances are still in the experimental stage and are not within the scope of this paper. The third type of noise is inherent in the receiver, being dependent upon receiver sensitivity, band width, and general design. This, of course, is determined by the functions which the receiver must perform.

The fourth type of noise is due to electrical transients in commutating machines, ignition systems, and other current-interrupting devices, which contain harmonic components extending into the radio-frequency spectrum. Satisfactory receiver operation dictates that this latter type of noise be suppressed or reduced to a point where the operation of electric equipment will have a minimum effect on radio communication. However, filter and shielding weight must be kept to a minimum. Therefore, radio design must be a compromise between desired radio performance and the limitations imposed by the requirements of installation, maintenance, and weight prevalent on present aircraft.

The recommendations and design procedures outlined in this paper are specifically for the frequency range of 0.16 megacycle to 20.0 megacycles. However, experience indicates that similar procedures, methods, and precautions may be applied to the ultrahigh-frequency spectrum.

In the following discussion, all wiring is considered unshielded except ignition circuits or those circuits where conduit is required for mechanical protection. The airplane is assumed to be of all-metal construction.

Radio-Noise Coupling to the Receiver

The radio-frequency energy components, coming from the various electrical transients, are induced into the receivers by various methods.

1. Radio-noise voltages generated by current-interrupting devices may be conducted into the receiver by means of the common power supply. This conducted noise is then coupled to the radio-frequency stages and results in noise in the receiver output.

2. The power wiring in the fuselage of the airplane conducts radio-frequency currents and voltages generated by the electric equipment. This results in radio-frequency induction fields which may be of large enough magnitude to induce, by magnetic or capacitive coupling, radio-frequency noise energy into the receiver-antenna leadin. Solution to this type of radio-noise problem is to reduce the mutual coupling by rerouting wiring and, if necessary, by electromagnetic shielding.

3. The ignition system of the airplane periodically (depending upon the engine speed and number of spark plugs) sets up steep-wave-front voltage discharges each time a spark plug is fired. This voltage discharge contains measurable components up to 450 megacycles.[2] The wave front of the discharge and subsequent oscillation is dependent upon the distributed circuit parameters in the high-voltage side of the ignition system.

The radio-frequency fields associated with the steep-wave-front discharges are shielded from the antennas by enclosing the magneto, distributors, and high-voltage leads with metal shielding. Efficiency of this harness shielding is dependent upon high conductivity of the entire metal enclosure. High-impedance joints between harness assemblies result in a decrease in effective shielding. Also, unless the harness is securely bonded to the airplane structure, the entire ignition harness will tend to radiate and produce resulting radio-frequency fields at the receiving antennas.

Designing for Radio-Noise Elimination

The performance of the radio equipment in the airplane is the final test of a satisfactory electrical and radio installation. Carefully tested electrical equipment which has been limited to a specified over-all radio noise voltage in the laboratory may, upon installation in the airplane, acquire a different radio-noise frequency distribution and unpredicted peaks.

It would then appear that laboratory testing is of limited value and that the prototype airplane must be tested and altered as required for satisfactory radio performance. The results of the tests could then be incorporated into the production airplanes. Although this method of design is good engineering, in actual practice there are many drawbacks to this method. Where production is of prime importance it is very difficult to effect changes after an airplane is in production. This is especially true in the case of electrical accessories. Installation of filtering on motors usually requires a redesign of the motor frame. Therefore, a compromise must be evolved whereby a maximum amount of radio design is accomplished prior to the completion of an airplane. This necessitates close co-operation between the radio engineer and the project electrical engineer.

Basic Radio-Installation Design

The following principles of design should be incorporated on the engineering drawings at the time when the electrical and radio systems are being developed:

(*a*). Antenna lead-ins must be kept as short and direct as possible. A compromise should be effected between structure shielding and a low capacitance lead-in.[3]

(*b*). The designer should strive for a maximum spacing between the antenna lead-in and electrical wiring.

(*c*). Great care must be exercised in bonding the receiver chassis to the airplane structure. The bonding straps must be as short as possible and the bonding must be made to basic structure. Too often radio-noise coupling to the antenna input circuit is a result of poor receiver bonding.[3]

(*d*). Sources of radio noise such as commutating motors and pulsing relays should not be installed close to radio equipment.

(*e*). All radio and interphone wiring should be in bundles separate from power wiring. A minimum separation of 12 inches should be maintained if possible between communications and power wire bundles.

(*f*). Antenna location on military aircraft is usually a compromise dictated by desired radiation characteristics, the need for a short antenna lead-in, and locations available in areas which are removed from the cones of fire of the guns. Consideration should also be given to locating antennas a maximum distance from the airplane engines.

Threshold of Conducted Radio Noise

It is desirable to reduce conducted radio noise to a level not greater than the internal noise of the receiver at maximum sensitivity.

The allowable conducted radio-noise level that will fulfill this condition must be known to permit proper filter design. This can be determined by the setup

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shown in Figure 1. Curves of power-line noise in microvolts versus receiver output in decibels can be obtained at various frequencies for each receiver. One such curve at 340 kilocycles is illustrated in Figure 2. From curves similar to Figure 2, a new curve can be derived of threshold of conducted radio noise versus receiver frequency. Threshold of conducted radio noise is defined in this paper as that value of noise voltage which increases the receiver output one half decibel above the internal noise with no signal applied. A curve of this type is shown in Figure 3. In this manner, the allowable conducted radio-noise level at the receiver can be determined in advance of the completion of the airplane.

Methods of Filtering Conducted Radio Noise

The choice of an airplane radio-noise-filter system should be governed by the following general considerations:

(*a*). The conducted radio-noise voltage level at the power input to the receiver must be within the applicable threshold levels.

(*b*). The coupling between the antenna leadin and the induction fields set up by the conducted radio noise must be kept to a minimum. Filtering exceeding the requirements of the threshold of conducted radio-noise curve specified in Figure 3, may be necessary to achieve a reduction in the radio-frequency noise induced into the antenna leadin.

(*c*). Filter weight must be kept to a minimum.

(*d*). Many changes in electrical and radio equipment are made on an airplane after it has been delivered to the customer. This problem should be recognized by the radio engineer and a safety factor allowed in his design.

There are a variety of approaches to the problem of achieving suppression of radio noise from electric equipment. General classifications are discussed as follows:

(*a*). *Receiver Filtering.* Filters may be inserted in the d-c power input to the receiver. The attenuation of the filter must be sufficient to reduce the radio-noise level below the threshold of conducted radio noise. However, this method has the following disadvantages:

1. Changes in electric equipment after the airplane has been delivered may result in a high radio noise level.

2. The requirement of low coupling of radio noise fields to antenna lead-ins is difficult to achieve without additional filtering on the power line.

(*b*). *Source Filtering.* Sources of radio noise have been limited by service specification to the equipment manufacturer to a value not exceeding 50 microvolts. This usually results in low radio-noise levels on the power lines. However, additional filtering may be necessary to eliminate resonant peaks. The objection to this method of filtering is the large amount of filter weight required. A source-filter installation on a large military airplane could subtract approximately 100 pounds from the pay load of the airplane.

(*c*). *Compromise Filtering.* This method is a compromise between the two methods discussed above. A limit of 500 or 1,000 microvolts can be specified for each noise source on the airplane. In the case of motors, this level can usually be achieved by the installation of a 0.1- or 0.2-microfarad capacitor across the brushes. The additional filtering is secured by the natural attenuation of the wiring and by capacitors located in appropriate junction boxes. A comparison between source and compromise filtering is shown by the curves on Figure 4. This test was conducted on a simulated-airplane-wiring network consisting of four motors, a receiver, and a power supply. Curve *A* represents the noise level at the receiver with each motor individually source filtered to a level not exceeding 50 microvolts at the motor. Curve *B* represents the over-all noise level at the receiver with each motor individually source filtered to a level not exceeding 1,000 microvolts at the motor.

Figure 4 indicates that very little advantage is derived from the use of heavy source filters. The 1,000 microvolt limit is not the final recommended value for this type of filtering. A value of 500 microvolts would probably be a more satisfactory compromise. In any event it would be desirable to use a level that could be attained by incorporating a small capacitor into present motor frames without necessitating a redesign of the motor. In designing for this particular type of filtering, space provisions should be allowed in junction boxes for installation of by-pass capacitors. Installations of these capacitors are determined at the time when the completed systems are tested.

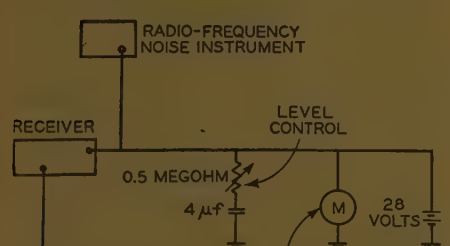


Figure 1. Laboratory setup for determining threshold of conducted radio noise for a receiver

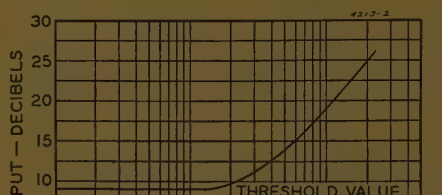


Figure 2. Typical curve of conducted radio noise on the receiver power supply versus receiver noise output for one frequency

340 kilocycles

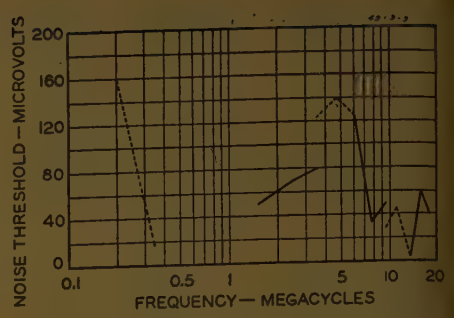


Figure 3. Curve of threshold of conducted radio noise for a receiver

The dash and solid lines indicate successive receiver bands

Ignition Radio Noise

The ignition system of the airplane sets up high-intensity radio-frequency induction fields in the vicinity of the airplane antennas. These interfering fields can be decreased by proper shielding of the ignition system. Shielding metal must be of sufficient thickness and high conductivity and each component part of the ignition shielding must be tied by means of low-impedance bonds to each adjacent member and to the airplane structure. Failure to have clean tight mating surfaces between component parts will result in ineffective shielding. On many airplane designs much engine testing is done on test stands prior to the completion of the airplane. If such is the case, an excellent opportunity avails itself to determine the suitability of the engine installation as regards radio noise.

Field-strength noise-instrument measurments can be made approximately ten feet forward of the propeller plane to indicate the severity of the ignition noise and to determine noise peaks. The ignition interference will usually resonate at approximately 9–10 megacycles. Upon determining the resonant frequency of the ignition system, the engine can then be probed to determine the efficiency of the bonding and shielding.

A Ferris model 32*A* noise instrument, or its equivalent, can be used with a probe to obtain a relative measure of the effectiveness of the bonding. A coaxial line of sufficient length and terminated with a sharp steel probe is attached to the Ferris noise instrument antenna input. The sharp point on the probe allows the test engineer to break through protective finishes on the metal in the probing operation. A ten-to-one radio-frequency voltage ratio from the magneto case to the high-voltage conduit would indicate a high-impedance bond between the two units. A high radio-frequency difference of potential between the engine and the airplane nacelle would indicate poor engine bonding.

After the incorporation of necessary

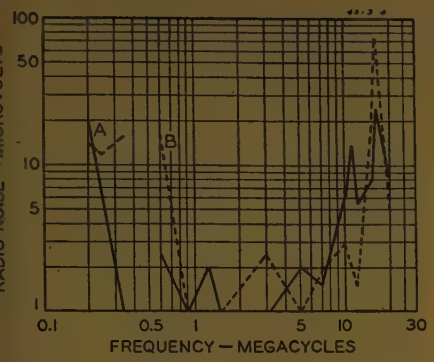


Figure 4. Comparison of source filtering and compromise filtering

A—Source-filtered installation

B—Source-, line-, and receiver-filtered installation

onding and shielding changes, the field-trength measurements are repeated to letermine whether additional work is ecessary. Experience has not yet de-ermined a satisfactory allowable field-trength level.

By following a procedure as outlined bove, necessary changes in the bonding nd shielding of the ignition system can e determined prior to the airplane's ompletion.

Testing the Completed Installation in the Airplane

With knowledge of the allowable con-ucted radio noise in microvolts for each eceiver, compliance with these values an be determined by measurement at he receiver power inputs. This test hould be conducted with various com-inations of equipment on the airplane perating. The combinations should ap-roximate as closely as possible service onditions. This is extremely important ince various combinations of equipment nay produce different levels of radio-oise voltage.

Antenna leadins must be carefully hecked for radio-noise pickup from the ower wiring in the airplane. An indi-ation of the magnitude of this pickup an be determined by listening to the utput of the receiver with the antenna eadin disconnected from the feed-through nsulator. As a result of this test, re-outing of wiring and further reduction n radio-noise voltage by filtering may be ecessary. However, in filtering, care hould be exercised against increasing he magnetic-induction radio-noise field y increasing the flow of radio-noise urrents. Field-strength measurements hould be repeated ten feet forward of ach engine and the results compared ith the values obtained on the engine est stand. If necessary, further probing n the engine should be accomplished.

Over-all performance of the radio quipment, or the true measure of the ffectiveness of the radio-noise design, is

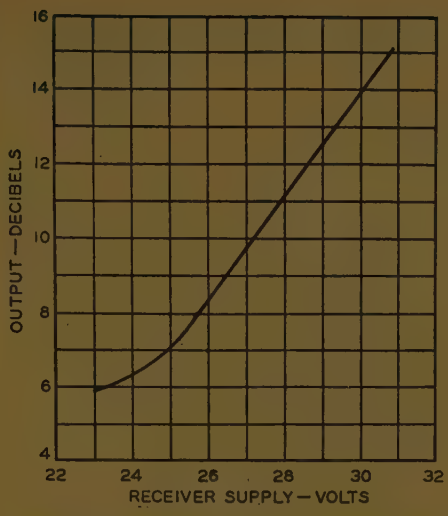


Figure 5. Variation of receiver internal noise with changes in receiver supply voltage

determined by the calibrated-receiver method. A receiver of the type to be used in the installation is calibrated in terms of a signal modulated 30 per cent at 400 cycles. Receiver output is measured with a quasi-peak-reading noise instrument of weighting circuit 10 milliseconds charge and 600 milliseconds discharge. Thus each receiver becomes an equivalent radio-frequency noise instrument. This arrangement combines the advantage of a noise instrument with the advantage of testing the actual receiver installation.

Accuracy of the calibrated-receiver method of radio-performance testing is dependent upon the following precautions and calibration technique:

(*a*). The power-supply voltage, both in the laboratory and airplane, must agree within plus or minus one half volt. Figure 5 shows how voltage variations affect the internal noise of a receiver.

(*b*). Circuit constants of the dummy antenna used in the receiver calibration should agree to within 20 per cent with the characteristics of the applicable airplane antenna.

(*c*). A minimum warm-up period of 30 minutes, should be allowed the receiver before any testing or calibration is undertaken.

(*d*). Calibration in the laboratory should be accomplished in a shielded room with a self-contained battery power supply.

By observing these precautions experience shows that results can be duplicated to within one decibel.

Readings of receiver decibel output in the airplane can be converted into equivalent microvolts from the receiver calibration curves. Maximum radio noise at the receiver input has been set by the services at 2.5 microvolts. Experience may permit a reduction in this value at a later date. Figure 6 shows comparative curves of an antenna measurement by the

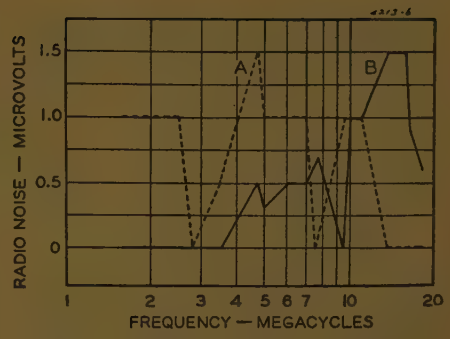


Figure 6. Comparison of the calibrated-receiver and noise-instrument method of measuring noise on an antenna

A—Noise-instrument measurement

B—Calibrated-receiver measurement

calibrated-receiver method and by a Ferris model 32*A* radio-noise instrument. The readings were taken as nearly simultaneously as possible. The curve of Figure 3 is for the calibrated receiver used in this test. It is interesting to note that the calibrated receiver minimum threshold values of conducted radio noise occurs in the range of 13–15 megacycles. This may explain the peak at 13 megacycles shown by the calibrated receiver in Figure 6. The Ferris instrument readings do not contain the effect of conducted radio noise.

Conclusions

1. It is possible, with knowledge of values of conducted radio noise that can be tolerated at the receiver and from tests conducted on test engines to complete a great deal of preliminary recommendations and designs which will decrease the delay in accomplishing a satisfactory receiver installation on production airplanes. The design, however, must be fluid to permit necessary changes during the tests on the prototype airplane.

2. A compromise system of filtering at source, junction box, and receiver is the most practicable and economical means of accomplishing radio-noise suppression within the fuselage of the airplane.

3. A calibrated-receiver method of evaluating the equivalent microvolts of noise should be the final test of the receiver installation.

4. Studies indicate that noise levels of 500–1,000 microvolts, if specified for electrical equipment installed in the airplane, would permit satisfactory levels of radio noise with a minimum filter weight.

References

1. A Study of the Characteristics of Noise, V. D. Landon. Institute of Radio Engineers *Proceedings*, volume 24, November 1936, page 1514.

2. Field Strength of Motorcar Ignition Between 40 and 450 Megacycles, R. W. George. Institute of Radio Engineers *Proceedings*, September 1940, pages 409–12.

3. Radio-Noise Elimination in All-Metal Aircraft, Fred Foulon. AIEE Transactions, volume 62, 1943, pages 877–91.

The Development of Aircraft Position Lights

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CONSIDERABLE interest has been shown in the development of flashing position lights as displayed on air-carrier aircraft at night and this paper is devoted to an explanation of changing trends in aircraft position lighting and the developmental work which finally resulted in the use of these flashing lights.

The need for a uniform system for the display and color of position lights on aircraft was soon demonstrated in the early history of aviation, particularly in Europe, where the proximity of many countries proved the need for uniformity. In October 1919 a convention met in Paris to discuss from an international point of view matters pertaining to aviation, and aircraft position lighting was one of the items which came up for consideration. Without going into any detail, it will suffice to say that it was decided that a red light be displayed on the left wing tip, a green one on the right, and a white light in the tail. The forward lights were to be visible through an angle of 110 degrees measured from dead ahead to the left and right, respectively, for the left and right lights, thus leaving an angle of 140 degrees for the tail light.

In 1926, the Civil Aeronautics Act was passed and when the Aeronautics Branch of the Department of Commerce was subsequently established, the requirements for position lights were drawn up which conformed with the principles as set forth at the Paris convention. Forward and rear lights were required to have a minimum intensity of eight candles and these requirements were in existence for years until the speed and number of aircraft operating at night had increased to the point where additional forward light was desirable, particularly for air-carrier aircraft. Accordingly, Aeronautics Bulletin number 7*E* as amended in October 1934 required that air-carrier aircraft display at least one stronger light to warn other aircraft of its presence. It was unfortunate that the requirement was so indefinite as it failed to specify the color, location, and the minimum intensity of the light. The result was that some one of the air-carrier operators inserted a 32-candlepower red lamp in one of the landing light units and this practice spread until it was used by all operators. The location of this light was not uniform because of the variation in the location of the landing lights, some being in the nose of the airplane and others in the leading edge of the wing. It was intended that this warning light be burned in conjunction with the forward lights but many of the pilots turned these lights on only when they knew that they were soon to pass an oncoming plane on the airway. The light thus was referred to as the "passing" or "courtesy" light.

The shortcomings of the warning light were soon apparent and in November 1937 the Civil Air Regulations provided for the display of a light on air-carrier aircraft having a minimum beam intensity of 35 candles directly forward. Difficulties in designing a light to meet the cutoff requirements resulted in a revision to the regulations in November 1939 which increased the minimum forward intensity to 40 candles while relaxing somewhat on the cutoff and stray-light requirements. By mid-January 1940, two manufacturers had obtained approval of air-carrier forward position lights and they were being shipped to purchasers. These lights were helpful, of course, but they did not solve the aircraft-lighting problem entirely. Congestion of aircraft over all of the larger airports where air-carrier traffic was predominant, disclosed the need for certain further improvements in aircraft position lighting. Pilots reported that they had some difficulty in locating and identifying aircraft as such which were ahead and below them. In many cases these were small slow aircraft engaged in night training flights. The pilots in overtaking ships claimed that the single white steady tail light on the aircraft ahead became one of thousands of city and airport lights which served as its background. In cases where the red or green wing tip lights were visible rather than the tail light, the lights were not easily picked up because of the number of colored neon lights in the background.

With the feeling that airplane conspicuity should be improved, at least from the rear, the Civil Aeronautics Administration was able to interest a Baltimore inventor in developing a device for flashing the tail light, the theory being that by so doing, it would be more readily discernible. The flasher was ready late in 1940 and was tested subsequently on a CAA airplane at Indianapolis by the technical development division of the CAA. The report of the tests contained many conflicting reactions but in general it was agreed that it was easier to locate the airplane by means of its flashing white tail light. Adverse criticism was mainly to the effect that many ground lights appeared as blinking lights to the airman because of obstructions which passed between the pilot's eyes and the source of the ground lights.

To gather additional information pertaining to the suitability of aircraft position lights, a questionnaire was prepared in July 1941 and sent to air-carrier operators and pilots, operating and engineering managers, and all interested parties. Replies were received from the following, as indicated:

Pilots	217
Engineering managers	6
Operating managers	11
Others	8

Some of the questions and answers were as follows:

1. Do you think the foreward lights (red and green) should be visible from a greater angle to the rear? Yes...(114) No...(106)

2. In your opinion, are the following currently used position lights adequate?

Air-carrier aircraft			
Forward lights	(40 cp)	Yes...(172)	No...(50)
Tail light	(8 cp)	Yes... (60)	No...(154)
Nonair-carrier aircraft			
Forward lights	(8 cp)	Yes...(27)	No...(168)
Tail light	(8 cp)	Yes...(46)	No...(145)

3. If considered inadequate, please describe in what respects.

As may be expected, there was a great variation in the answers but in general they indicated that greater-intensity lights were desired; that the tail light particularly should be brighter; that there should be no difference between light intensities on air-carrier and nonair-carrier aircraft; that all forward lights should have a minimum intensity of 40 candles; and that the tail light should be made more distinctive by the use of a blinker and a different-color light.

The next question is particularly interesting as it indicates to what extent pilots rely on their position lights to aid them while flying at night.

4. To pilots. (1) Do you make much use of the color arrangement on another airplane to determine its directional path or (2) are you interested only in knowing of its presence?

There were 176 who stated that they made use of the color arrangement to determine an airplane's directional path and there were 70 who were interested only in knowing of its presence. It will be noted that the total number of opinions exceeds the number of replies. This was due to the fact that some of those who replied stated that they were interested in both. They were primarily interested in knowing of the airplane's presence and then they determined

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The views stated in this paper are those of the author and not necessarily those of the Civil Aeronautics Administration.

ts path by means of the arrangement of the colored lights.

5. After giving due consideration to the possible need for larger sources of electrical energy, to the resultant increase in weight, and the possible effect on the aerodynamic performance of the airplane, do you have any suggestions as to how to improve the position light or lighting system on aircraft?

The suggestions for improvements were interesting and extremely varied. Many of the pilots mentioned neon, "lumiline," and fluorescent lighting. They suggested that such lighting be used in the wing tips, the trailing edges of both wings, and control surfaces. Some suggested that these lights be of the yellow sodium-vapor type, while others suggested that they be of the blinking type. In general, there was a demand for more light—either higher intensity or more lights—or both. Others wanted two additional lights in the nose of all aircraft. Several frankly stated that the airplane should be "lit up like a Christmas tree" and one felt that it "should stand out like a ferry boat on a river."

By interpretation of the results of the questionnaire, it was obvious that the pilots were generally dissatisfied with the effectiveness of the position lights. They wanted the lights to have a longer range which meant greater intensity and since so many mentioned distinctive lights and the inability to discern the aircraft against a background of city lights, it appeared that an increase in conspicuity was necessary, particularly for the tail light, as well as a more definite means of identifying the light as belonging to an airplane.

How the inability of a pilot to ascertain that a light belonged to a plane was contributary to an accident was forcefully illustrated about this time. A pilot mistook the steady white tail light of another air-carrier plane on the ground as being a boundary light some distance away and he started his take-off run towards the light. After considerable speed had been attained, the pilot became aware that he was about to crash into a plane. He immediately throttled his engines, swerved the airplane, and put his control wheel hard over, thus attempting to lift his wing over the other plane. In this maneuver he was partially successful although he did strike the outer portion of the airplane's wing.

This accident gave additional impetus to the problem of improving the safety of night flying and it was decided:

(1) That some research be undertaken immediately towards giving the tail light greater conspicuity,

(2) That some work be done towards developing a light which would make air-carrier aircraft more discernible from the front.

The need for the latter was particularly acute because of the danger of collison with Army and Navy aircraft, which for military reasons displayed little and sometimes no light. That this hazard was serious can be illustrated by quoting an operator who stated that within a relatively short time he had 178 military aircraft using the airways ordinarily flown by his pilots.

The problem of building some test units which would permit two lights to be flashed at different cycles and eclipse periods was left to the National Bureau of Standards which was ready for tests during the latter half of August 1941. Approximately 20 observers were present and they represented the Air Transport Association of America, a few air-carrier operators, manufacturers of flashing devices, the National Bureau of Standards, the CAA, and the Civil Aeronautics Board. Constant telephonic contact was maintained between the observers and the personnel in control of the lights on the roof of the East Laboratory on the Bureau of Standards grounds. With the equipment as set up, it was possible to vary the frequency from 30 to 40 to 50 cycles per minute and to change the length of the eclipse periods. The observers were approximately six-tenths of a mile away and all were equipped with pad and pencil to record their reactions to the various light flashes.

Using red and white lights which had been determined in some preliminary observations a few nights earlier as having the greatest penetrating power, the result of the evening's work indicated that alternately flashing red and white lights were best when flashed at a frequency of 40 cycles per minute with the following characteristics: 150 degrees white—10 degrees dark; 150 degrees red—50 degrees dark. The longer dark period was found to be necessary in order to make the following white flash appear as such rather than with a pinkish tint. The eye seemed to retain the red image longer than the white with the result that the dark period had to be lengthened until the point was reached when the following white flash no longer had any pinkish cast to it. It was found that even with 21-candlepower lamps in the units, the red flash lacked intensity and it was necessary to use a 32-candlepower lamp. Subsequently, in order to eliminate the need for stocking two tail lamps of different ratings, a 32-candlepower lamp was used in each unit.

With this much accomplished, the manufacturers of flashing devices had some definite requirements on which to base the design of their flashers. While they were engaged in this work, American Airlines played an important part in the development which led to the present scheme of flashing lights. During the last few months of 1941 and the first in 1942, this operator worked closely with the National Bureau of Standards and the CAA in a number of tests trying out various arrangements of steady and flashing lights. One of these tests was conducted at Washington in January 1942 and was witnessed by approximately the same representatives as participated in the earlier preliminary tests at the National Bureau of Standards. Pennsylvania-Central Airlines, which had been following this lighting project with interest by participating in the various preliminary discussions and tests, furnished a *DC*-3 airplane in which the observers rode to observe the relative conspicuity of the American Airlines airplane with and without the various flashing-light combinations. Additional tests were conducted in February while two American Airlines airplanes flew between New York, N. Y. and Hartford, Conn. During a test on February 25, 1942, a flasher was installed which gave the correct number of cycles and eclipse periods. The results convinced the majority of observers that flashing the red and green wing-tip lights simultaneously with the white tail light and the top and bottom white fuselage lights with the red tail light, gave the best results. The tests also led to the final approval by the CAA of a flasher which had been tested for the flashing schedule, satisfactory operation during three hours of vibration, high- and low-temperature effects in the range from plus 55 degrees centigrade to minus 35 degrees centigrade, radio interference, and operation with variation in input voltage. The motor-driven mechanism required about 300 watt-hours of energy per year for its operation and units are now available which can be used on 12- to 16-volt or 24- to 28-volt systems, although the first unit was designed for use on a *DC*-3 which had a 12- to 16-volt system. In appearance the flasher is a cylinder having a maximum diameter of $4\frac{1}{2}$ inches and a height of $6\frac{1}{4}$ inches. Its weight is two pounds.

With the approval of this flasher, American Airlines proceeded to install this system of six flashing lights in May 1942. The co-operation of the Air Transport Association was obtained in soliciting its members to notify their pilots to observe this system of lights in order to determine their effectiveness. The pilots apparently were impressed because in November 1942 the operations committee of the Air Transport Association voted unanimously to request the CAA that all air-carrier operators be permitted to install the system of six flashing lights. Naturally this reception was gratifying and permission was given with the proviso that it be uniformly adopted by all of the operators. It should be borne in mind that the Civil Air Regulations which had been revised in February 1942 required only that a flashing red and white tail light be displayed and, therefore, the understanding with the operators was strictly on a "gentlemen's agreement" basis. Although the date of effectuation of this requirement had to be postponed several times because of a number of difficulties, progress in completing the

installations was satisfactory considering the circumstances. With the approval of another flasher in August 1942, an additional source was available which expedited the completion of the flasher installations.

By the end of 1942, practically all air-carrier aircraft were flashing three lights on each of the two flasher circuits with each carrying about $5^1/_4$ amperes. One circuit operated the standard color scheme, namely, the red and green wing-tip lights and the white tail light, while the other operated the top and bottom white fuselage lights and the red tail light. Not only was aircraft conspicuity greatly improved from the rear but from the front and sides as well and all pilots were impressed with the added sense of security. Apparently the Air Transport Command thought well of the system also because in December 1942 the CAA was informed by one of the flasher manufacturers that his units were being installed in a number of transport and cargo airplanes operated domestically by the Air Transport Command. These aircraft were flying the airways at night and the desire to maintain the high safety standard then in existence dictated that flashing lights should be installed. The value of flashing lights was accorded additional recognition nine months later when a military requirements policy issued by Headquarters, Army Air Forces, authorized the installation of a flasher on six certain types of mi itary aircraft permanently assigned to activities within the continental United States. However, the authorization did not permit the aircraft to be altered to the extent of adding the white tail light and the top and bottom white fuselage lights. Presumably, the aircraft were badly needed and could not be withdrawn from service to enable the additional lights to be installed. At present the Navy is adopting the standard air-carrier system of six flashing lights for use on its domestically operated transport aircraft and it is our understanding that some aircraft are already so equipped.

This entire development has been aimed at maintaining the high standard of safety while flying at night on air-carrier aircraft. However, sight was not lost of the fact that nonair-carrier aircraft engaged in night flying were potentially just as dangerous to each other and to other air-carrier aircraft. From its very inception, the plan contemplated that something be done eventually to add greater conspicuity to privately owned aircraft of all sizes but it was decided that this program would be subordinate to the other. Now that the air-carrier aircraft are provided with adequate position lighting, steps have been taken to initiate something similar for other aircraft. One of the first things required is a simple, cheap, flashing device. The present devices are somewhat in the nature of precision instruments and cost about $95. A material reduction in price will be necessary before the average private flyer can afford a device of this kind. The need for a simple cheap flasher to cost about $15 was brought to the attention of several manufacturers who are now engaged in working on this problem. The unequal dark periods, namely, 10 degrees and 50 degrees, have added some complication and have been responsible, in part, for discarding certain mechanisms which otherwise might have been suitable as considerably cheaper flashing devices. With the thought in mind of simplifying the design problem, it is our intention to experiment with a flasher having both dark periods equal. The important dark period is the one after the red flash, so it is planned to have both eclipses equal to 50 degrees and to decrease the "on" periods from 150 degrees to 130 degrees. The cycle would thus be 130 degrees white—50 degrees dark; 130 degrees red—50 degrees dark. It is not believed a reduction in the "on" periods would be noticeable.

In addition to a cheap, reliable flasher, a single unit housing both the red and white tail lights is necessary before flashing lights will be commonly used on nonair-carrier aircraft. Lack of space on the rudder, the tail post, or tail cone of small airplanes will make it impossible to install two tail lights of the type currently used adjacent to each other. At least one company is known to be working on this project. Eventually it is hoped that nonair-carrier aircraft operating at night will flash as a minimum their wing-tip lights with each flash of the red and white tail lights.

The Civil Air Regulations have been revised recently to permit the flashing of the red and white tail light on nonair-carrier aircraft. Further revisions appear in order to cover similarly the flashing of the wing-tip lights and the top and bottom white fuselage lights. Before this step is taken and because the use of flashing lights appears destined to spread, a number of questions arise which are in need of further study. For instance, the point has been raised that since the red and green wing-tip lights are used to designate the forward left and right quarters, respectively, the introduction of a red light in the tail is unorthodox and may under certain conditions cause confusion. It has also been pointed out by those interested in international agreements that the use of flashing lights and the introduction of red in the tail is contrary to such agreements. Although this has been handled satisfactorily by the operators whose craft engage in foreign operation, by the use of a switch converting the wing tip and white tail lights to steady lights, the subject may warrant some further consideration inasmuch as a long-range policy for aircraft lighting should be considered.

A short questionnaire has been prepared soliciting some information from the air-carrier operators relative to the reaction of their pilots to the flashing lights now that they have been in use for about two years and it is hoped that the information received will indicate if any major changes in aircraft position lighting are necessary.

Since the presentation of this paper, the replies to the above mentioned questionnaire have been received. Fourteen of the 17 domestic air carrier operators have replied, as indicated, to the following four questions which are the most important ones of the questionnaire:

1. Are you satisfied with the present scheme of flashing the two wing tip lights and the tail light on one side of the flasher circuit and the top and bottom white fuselage lights with the red tail light on the other? Yes 14; no 0.

2. The aircraft's progress with such lights flashing has been described by a few as being "jumpy." Is this a serious objection to your pilot personnel? Yes 0; no 14.

3. Does the red light in the tail of an aircraft confuse your pilots as to its direction to an objectionable extent? Yes 2; no 12.

4. Do your pilots think the forward red and green lights should have more intensity? Yes 3; no 11.

On the basis of the replies, it is obvious that the operators as a whole are well satisfied with the present scheme of flashing lights. No changes are thus contemplated, although it is felt that the volume of air traffic in the not too far distant future will force a revision in aircraft position lighting. Such a revision will call for a pattern of flashing lights enabling a pilot to distinguish more readily the quarter from which he is approaching another aircraft.

Electric Gun Turrets for Aircraft

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THIS war has seen an unprecedented development of armament, both offensive and defensive, for military aircraft. One of the most widely used and, therefore, perhaps one of the most important of these has been the power-driven gun turret, three or four of which are used on all our heavy bombers, and one or more on most smaller bombers and equivalent planes. This paper briefly surveys the field of electric-drive equipment for turrets to give a picture of the types and variety of equipment now in use and the requirements made of it, together with a description of how it works.

General Description of Turrets

Basically a turret is a power-driven flexible gun mount. In most turrets now in use the gunner is located within the turret, and by suitable controlling means drives it at any desired speed in rotation about horizontal and vertical axes so as to point the guns continually at his moving target. The gunner actually rides around with the guns and aims by peering through a sight attached to them.

In this connection it would be well to mention the two most prevalent basic types of turret arrangement. These are, first, the cylindrical turret wherein the complete turret with operator, ammunition, and so forth revolves only about a vertical axis, the guns alone being pivoted about a horizontal axis in the turret for elevation and depression. The gunner has to move his head up and down to follow the sight in elevation. The other type is the spherical turret where the gunner is at all times in a fixed position with respect to the guns, and rides up and down with them in elevation as well as around in azimuth. In both cases transparent Plexiglas enclosures through which the guns protrude are used to shield the gunner from the slipstream. Turrets of both types have been applied variously in nose and tail installations, as upper deck turrets, forward and rear, and as lower turrets projecting from the bottom of the airplane fuselage.

Much of the basic turret development was done prior to or in the early days of the war, being pioneered to a considerable extent by the British who concentrated on the caliber-0.30 automatic machine guns and used them in multiple gun installations, usually numbering four guns. American turrets from the first have used the caliber-0.50 machine gun in arrays of from one to four. By far the greatest majority, however, have had two guns, one on either side of the gunner. Larger caliber armament including 20-millimeter and 37-millimeter cannon has been used, but mostly experimentally to date.

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Power-Drive Requirements

The necessity for power drives in aerial gunnery is immediately apparent from the desire for high fire power which demands more, larger, and heavier guns, and from the forces imposed on the guns by the wind velocities encountered with modern high-speed airplanes. Wind loadings on a twin caliber-0.50 turret for a modern bomber have been checked at between 300 and 400 foot-pounds of torque, varying roughly sinusoidally as the turret is rotated through 360 degrees. In addition, such a turret with full complement of ammunition, armor plate, and accessories may weigh in the neighborhood of 700 to 1,000 pounds, which must be accelerated quickly to meet the requirements of aerial combat. The top speed generally required of aircraft turrets is about 10 rpm, which represents a figure somewhat over 0.5 horsepower. Larger guns take up to 1 or 1.5 horsepower. Obviously, a human gunner could not maneuver the guns to any reasonable degree without the aid of a power drive.

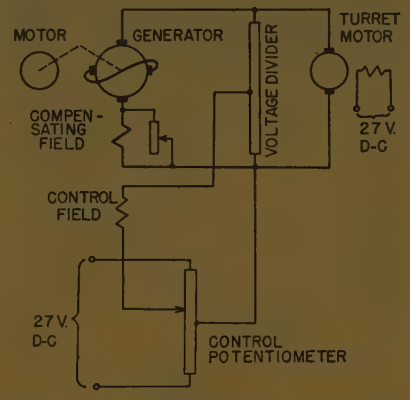


Figure 1. Basic Amplidyne control circuit

The requirements for a turret drive are quite stringent. The guns must be under perfect control at all times if accurate shooting is to be accomplished, and no looseness in the response to the controls is to be tolerated. Perfectly smooth steady operation must be obtained over the entire speed range from zero to the top speed of the turret, and the turret speed should be as insensitive as possible to the changes in wind loading when it revolves. High accelerations and quick reversals must be obtainable, and the equipment must be light, simple, trouble-free, and easy to maintain. In addition, it must perform equally as well at altitudes of 40,000 feet and ambient temperatures of −65 degrees Fahrenheit as it does under normal conditions or under conditions of desert heat. Of course it must withstand the shocks and vibrations incident to use on military aircraft.

Types of Drive Equipment

Both hydraulic and electric-power-drive systems have been used successfully for turrets. The hydraulic drive was long favored because of the smoothness and slow speeds which were obtainable, and in more powerful designs because of its light weight. However, control difficulties, problems of leakage, cold weather troubles, and vulnerability have militated against it to an appreciable extent. At the present time, in this country at least, hydraulic turrets are in the minority.

The wide range of load and speed required makes an unusual control problem for aircraft electrical equipment. To date the most successful solution to it has been use of the Amplidyne type of generator, and by far the greatest majority of electric turrets are now Amplidyne controlled. The Amplidyne is especially suited for turret control because of its quick response and high power amplification. These features permit accurate control and the use of a very small amount of control power, which means the utmost in small and simple control devices.

Amplidyne Control Circuit

Figure 1 illustrates the circuit in general use with Amplidyne turret drives for controlling the speed of the turret-drive motor. It is fundamentally a variable-voltage system with negative-voltage feedback for regulation and stability. The controlling means is a center-tapped

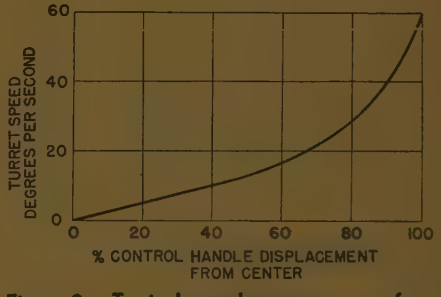


Figure 2. Typical speed-response curve for a turret

potentiometer fed from the airplane's 27-volt d-c supply, and exciting the low-wattage control field of the Amplidyne. Turret motor speed is controlled by varying the magnitude and polarity of the Amplidyne field excitation. The turret motor is a separately excited shunt machine, so it is necessary to compensate the Amplidyne (by means of a series field) somewhat over 100 per cent to overcome *IR* drop and inherent speed regulation of the motor, and thereby maintain good turret speed regulation under high loads. Such a system would be unstable by itself at lower speeds and loads, and so a portion of the Amplidyne output voltage is fed back into the control field circuit for stabilization. This is obtained from a resistor which constitutes a voltage divider across the Amplidyne output. The field excitation is the difference between the potentiometer output and the voltage from this divider. The position of the divider tap determines the Amplidyne output voltage for any control potentiometer setting, making this in effect a speed regulator circuit. With a linear control potentiometer the turret speed characteristic, with respect to control handle displacement, is a straight line. This is quite positive and gives both high acceleration of the turret in response to the control handles and a dynamic braking effect for holding the turret steady against the wind, even when the control potentiometers are not energized.

With the machines commonly in use this circuit has a time constant of response of about 0.04 second and can accelerate a turret up to 500 degrees per second per second. Because of the high inertias involved at such accelerations, it is necessary to antihunt the system. To accomplish this an additional field is connected across the Amplidyne output through a capacitor and is of such polarity as to buck down any voltage produced by the control field. This arrangement merely prevents an oversensitive Amplidyne from accelerating the turret too fast.

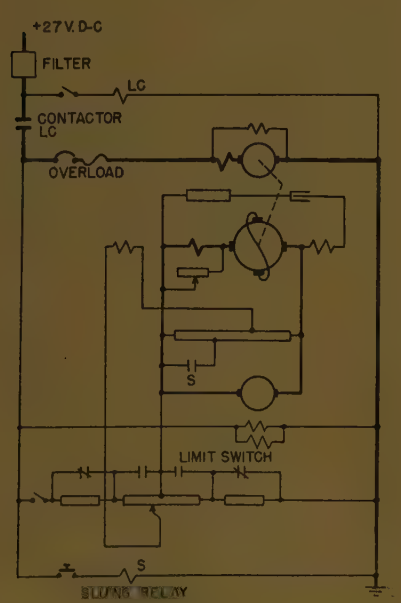


Figure 3. Typical turret-drive elementary diagram

Sluing Circuits for Amplidyne Control

The basic circuit described previously is readily adaptable to modification by several types of additional control and safeguard circuits useful in turrets. One of the most important of these is a sluing circuit, which amounts to a gear shift for tracking speeds. When tracking a target the gunner seldom needs to operate the turret at a high rate of speed, and it is not desirable to have too great a change in rate for a given motion of the control handles, as this makes control more difficult. For tracking, a voltage divider tap is used which gives a maximum turret speed of 20 or 30 degrees per second. But the gunner also wants to be able to shift rapidly from one target to another, that is, slue the turret. This can be accomplished by changing the voltage divider tap so as to obtain full Amplidyne output. If desired, the tap can be changed gradually by means of another potentiometer geared to the control handles so as to change the slope of the response curve gradually, as shown in Figure 2.



Figure 4. Amplidyne for turret drive and speed control

Limit Circuits for Amplidyne Control

In addition, a limit circuit is often desirable to prevent the gunner from running the guns down full speed into the side of the airplane. Such a circuit must have an automatic backout feature, and it must be simple. It is accomplished easily by having a limit switch short circuit that side of the control potentiometer which drives the guns toward the limit.

Miscellaneous Circuit Modifications

Generally an "action switch" or "deadman" is incorporated in the control handle to cut off power from the control potentiometers when the handle is released. This is a safety feature to prevent the turret from swinging around in the event that the controls should be knocked accidentally when power is connected to the turret.

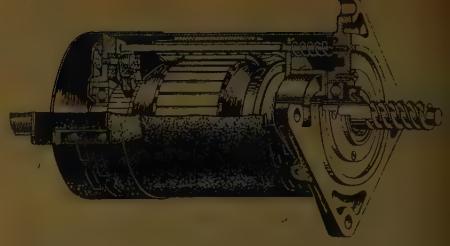


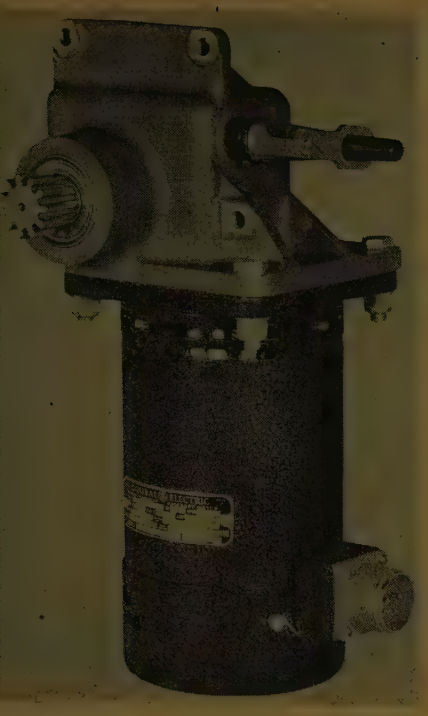
Figure 5. Sectional view of typical turret motor showing brake coil and mechanism near pulley end

It often is considered desirable to have brakes on the turret to prevent its swinging around in the wind when power is off. A nonreversible worm drive would accomplish the same thing but is rather inefficient. For this purpose some turret motors have brakes built into them, which release magnetically when power is applied to the field.

Figure 3 shows a typical turret control circuit elementary for the elevation motion. The Amplidyne is driven by a compound-wound motor directly off the ship's 27-volt d-c supply. It is started by a relay, has thermal overload protection, and there is generally a radio-noise filter in the line. The turret motor field and brake are energized at the same time the Amplidyne is started, so as to prevent circulating currents flowing around the loop circuit between generator and motor armatures. The control potentiometer is energized by a separate "action switch," and sluing is obtained by means of a relay. Upper and lower limit switches with current-limiting resistors are included. Miscellaneous accessories are not shown.

Turret Amplidynes

The Amplidynes now in general use are two-bearing motor generator sets 12 inches long and 29 pounds in weight (see Figure 4). The motor is compound-wound with eight poles, rated at 27 volts and 44 amperes direct current. It runs in ball bearings at 8,300 rpm. The generator is rated at 60 volts, 8.8 amperes (530 watts) for ten minutes, and at this output requires about 60 milliamperes in the 25-ohm control field (0.1 watt). These machines also have a $1\frac{1}{2}$ minute rating of 750 watts, corresponding to the maximum wind load torques on the turret, and will put out over 1,000 watts for short times, necessary for breaking away a frozen or iced-up turret. Because it is expected that they will not have long life in such an application, the temperature rise generally is considered to be limited by what the commutators will stand before throwing solder. All connections are made



gure 6. Turret motor and gearbox showing inion to mesh with turret-ring gear and lever to declutch motor

Gear reduction: 10 to 1

hrough a single *AN* connector, and two heostats are mounted on the side for hunt adjustment of the compensating eld. A fan is located on the motor end f the machine, and all rotating parts are alanced dynamically. Vibration is quite nportant insofar as it affects generator ommutation, which is extremely critical r smooth turret operation and correct ompensation. Special brushes are used r satisfactory life at high altitudes, consistent with reasonable commutator and rush life at sea level.

Turret Drive Motors

The drive motors for the turrets are ated at 60 volts armature and 27 volts eld, and generally are rated at 4,000 pm. Good performance is required, owever, over the entire speed range and articularly at very low speeds. Sticky earings or any tendency for slot-locking ill give ragged turret performance at he lowest speeds where accurate aim is sually most important. The motors ust be able to take whatever the Amlidynes can put out, and this often may ean almost instantaneous reversal of aximum applied armature voltage. Of ourse they must run equally well in both irections, and the maximum speeds in pposite directions should be essentially qual. When space permits they are supied with a fan for cooling, but its value questionable, as the motors must run low speeds and full torque for much of he time. A typical turret motor is shown Figure 5. It is seven inches long and weighs about ten pounds. Its ten-minute rating is 0.5 horsepower at rated voltage and 8.3 amperes. Figure 6 shows a similar motor mounted to a gear reduction box for driving a turret. Note that a lever is provided to declutch the worm-drive gearing to make the turret easier to rotate manually in the event of power failure. The over-all efficiency of a drive system using such motors and Amplidynes is in the neighborhood of 40 per cent (not including turret gear efficiencies).

Control Units for Turrets

Satisfactory control equipment for easy, efficient, and accurate manipulation of the guns requires a carefully designed control unit. The controls above all must be simple and "natural;" that is, their arrangement must be such that their correct operation is virtually instinctive, with as little as possible for the operator to learn. They must be operable with the minimum expenditure of thought and energy on the part of the gunner, and they must be comfortable and easy to use. When it is considered that the gunner may have to operate the turret for many hours on end, often under conditions of extreme temperature and altitude, it is understandable that much consideration must be given to the ease and comfort of his work if his efficiency is to be maintained.



Figure 7. Joy-stick-type control unit for spherical turret



Figure 8. Typical turret control unit with standardized grip arrangement

Many types of control handle arrangements have been tried. Steering wheels, single or double joy-sticks, rotating grips as on a motorcycle, and various types of handle-bar arrangements all have been used. There is no doubt that operation of most any arrangement can be learned, and also that different turret arrangements may require different control arrangements. Figures 7 and 8 illustrate two typical control units, one a joy-stick type with pistol grip, and the other a double-grip arrangement. The former has been quite satisfactory in a spherical-type turret. For cylindrical turrets the double-grip arrangement of Figure 8 has been found most satisfactory, and it has been standardized wherever possible. In this arrangement both grips move together and rotate on a horizontal shaft for elevation control and about a vertical axis midway between them for control of azimuth rotation. The two grips customarily are placed close together so as to be operated primarily by wrist motion, so as to minimize fatigue. Likewise, friction and inertia should be low, and any spring centering kept very light. However, there still must be enough friction and inertia to give the operator something substantial to hold onto, when the airplane bounces or maneuvers, without upsetting his degree of control. The inertia of the operator's arms and body is important here in creating a control system which is not subject to jerking during acceleration.

In this typical control unit the grips are of plastic material. Each grip has a lever on the side which, when depressed by the weight of the gunner's hand, energizes the control circuit. There is also a gun-firing trigger switch on each grip, operated by the forefinger, and a push button accessible to the thumb for sluing, or for an interphone if desired. The grips can rotate $22\frac{1}{2}$ degrees either side of the center position for both elevation and azimuth control and are geared directly to the control potentiometers. The latter are of sufficient diameter and close-wound with fine enough wire to give smooth control without noticeable steps in the change of speed. The upper portion of the unit is devoted to a control panel with switches, rheostats, and overload resets to control the sight, firing circuits, and miscellaneous accessory equipment. Connections are made through standard *AN* connectors and fittings.

Electrical Accessory Equipment

The electrical gun-firing and accessory equipment being used on turrets nowa-

Influence of Electricity on Aircraft Instrumentation

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Synopsis: Many factors have operated to set up conditions which have been very favorable to the electrification of aircraft instrumentation. Two tabulations are presented to indicate the extent to which electrical principles have been applied to common aircraft instruments. The effects of increased size in aircraft, larger power plants, longer flight time, association with other equipment, and other factors upon instruments are discussed. The advantages of electric instruments are presented, and some possible future developments are noted.

CURRENT bibliographies on aircraft instruments list a reference to an early article dated 1904—40 years ago. This paper, describing an altimeter, has been followed by thousands more indicating the interest and technical advancements in aircraft instruments. The needs of the aircraft industry have been met by the instrument manufacturers. Developments in instruments have paralleled the rapid developments in aircraft due to the close co-operation of measurement and aeronautical engineers. A typical aircraft instrument panel is shown in Figure 1.

Since few manufacturers make a complete line of aircraft instruments, an overall listing of the devices included in this field is difficult to find. Textbooks, bulletins, and trade catalogues list instruments under various classifications. The devices covered and the typing are seemingly influenced by popular terminology and the author's particular interest. Another classification is not, then, out of order.

Aircraft instruments can be identified by the function which they perform. A tabulation made on this basis should be of interest in cross-checking the needs of the aircraft with the instruments available. Aircraft instruments can be grouped by the type of instrument and by the basic principle of operation.

Because many factors have operated to set up conditions which have been very favorable to the electrification of aircraft instrumentation, particular emphasis can be placed on this effect by segregating the electric instruments from other types.

The increase in use of electric instruments is traceable to the following factors:

1. Increase in size of aircraft.
2. Increase in size of power plants.
3. Longer flight time.
4. Desirability to fly under adverse conditions.
5. Nonelectrical methods not feasible.
6. Association with other equipment.
7. Adaptability to available power source.

Since electric devices, other than instruments, are being used in increasing numbers on aircraft, it is anticipated that electric instrumentation will progress.

Scope

An instrument is normally considered a measuring device. In the control of any process, regardless of its nature, a measuring device may function in two ways:[1]

1. It may give visual indication of events or conditions which may govern the manual adjustment of apparatus controlling these events or conditions.
2. It may initiate actions which automatically adjust equipment controlling events or conditions.

Only instruments in the first group are considered, since control devices such as voltage regulators, fuses, and speed-control apparatus are included in the second group. The aircraft industry operates in general within this definition. Two exceptions are warning lights, which are not considered to be instruments, and automatic pilots, which are classed as instruments. Interlocking operation, so easily accomplished by electrical means, makes the distinction between an instrument and a control device increasingly difficult.

Classification

Table I shows aircraft instruments by basic type of measurement. This table may not be comprehensive, that is, it may not include all instruments, but it does show all instruments used in any quantity. Automatic pilots are not tabulated, since their function other than control is duplicated by other gyro instruments.

Table II shows the aircraft instruments segregated into their classes: flight, navigational, engine, and miscellaneous with the basic principle of operation listed. Further classification is made by dividing the instruments into groups indicating that the instruments operate as self-contained units or consist of two units, a transmitter and a receiver, to give remote indication. The means by which the basic principle of operation is effected is also shown.

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days has reached considerable proportions. Guns are fired almost without exception by electric solenoid devices mounted on the guns, and drawing from four to eight amperes. Usually both guns are fired simultaneously, although sometimes provision is made for separate firing of the guns when desired. Cam-operated fire interrupter switches are used in these circuits to cut out the gun fire in interference regions, so that gunners will not inadvertently shoot away the tail or wings of their own airplane.

Because of the large amounts of ammunition carried, the feeds to the guns are long and tortuous, so that ammunition boosters are required. These are ratcheted sprockets powered by small intermittently rated electric motors which operate when tension in the ammunition belt reaches a predetermined value. They generally take considerable current; 20 amperes is not unusual.

Automatic electrically driven movie cameras aimed along the line of fire are often used to record the effect of the gunfire. They are energized whenever the firing trigger switches are closed.

Additional electrical accessory equipment often used includes the interphone, a rheostat-controlled power outlet for electrically heated clothing, defrosting heaters and fan for the Plexiglas dome, and usually a "trouble light" on the end of a flexible cord for general illumination.

The total power drain of a 27-volt turret with everything running at once, and going at full speed against the wind may be well in the neighborhood of 175 amperes.

Conclusion

Amplidyne turret drive and control equipment is now in use on thousands of turrets of several different types and manufacture all over the world. It lends itself readily to many control-circuit variations and mechanical arrangements and is eminently simple and easy to service and maintain. All reports have indicated that it is thoroughly reliable and performs its proper functions satisfactorily.



Figure 1. Typical aircraft instrument panel

There are some measurements, such as exhaust-gas analysis, which are made only by electrical means. Some measurements, such as time, are made only by mechanical devices.

It is indicated that many measurements can be made by devices utilizing both mechanical and electrical principles in their operation. While certain instruments, fuel-quantity gauges, for example, can be obtained operating on more than one principle, general usage is in favor of those utilizing electrical means. Figure 2 shows the relative quantity of instruments available in the various classifications.

Factors Favoring Electric Instrumentation

Airplane Size

The size of aircraft has increased tremendously. Wing spreads over 200 feet are not uncommon. Engines are placed out on the wings with the obvious result that engine instruments are principally electrical in operation or utilize electric transmission. Electric tachometers eliminate the impossible task of using flexible cable drive through a distance of as much as 60 feet between the engine and the instrument panel. The saving in weight by eliminating the use of cable is many pounds. Synchronization of multiple engines is indicated easily by electric synchroscopes operating from tachometer generators.

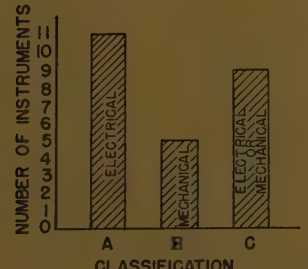


Figure 2. Summary showing relative quantity of instruments in various classifications

Many pressure indicating devices utilize electrical transmission to eliminate long heavy pipe lines, thus reducing installation costs, maintenance difficulties, and operational hazards by avoiding the danger of leaks and by decreasing vulnerability to gunfire.

Other parts of the airplane are often so far from instrument or control panels that the electrical method is the only practical means of measurement. Gasoline tanks are multiple in number and placed in all parts of the airplane. Landing gear, flaps, and so forth cannot be seen by the pilot, and position indication must be provided.

Remote-indicating electrical compasses[2,3] are a necessity because of the impossibility of maintaining a uniform magnetic field in the cockpit. The pickup element must be located in a remote position, such as the tail or wing, to secure accurate readings.

Much has been written on the subject of power systems in aircraft. Increased power loads and the use of 400-cycle supply dictates the use of ammeters, voltmeters, wattmeters, and so forth, to assure proper control.

Table I. Classification of Aircraft Instruments

		Specific Measurements	Instruments
1. Measurement of Quantity			
Absolute	Solids, liquids, gases	Fuel quantity	Flowmeter
		Airplane height	Radio altimeter
		Ice accumulation	Rate-of-ice-accumulation indicator
	Electricity	Electric load	Ammeter
With respect to time	Power, Mechanical	Engine horsepower	Horsepower indicator
	Power, Electrical	Generator load	Wattmeter
2. Measurement of Quality			
Composition analysis		Fuel mixture	Exhaust-gas analyzer
3. Measurement of Condition			
Temperature		Outside air, Oil, Coolant, Carburetor mixture, Inside air	Thermometer
		Engine cylinder, Jet exhaust	Thermocouple
Pressure		Oil, Fuel, Manifold, Suction, Deicing equipment	Pressure gauge
Speed		Engine rpm	Tachometer
		Engine synchronism	Synchroscope
		Airplane rotation	Rate of turn indicator
		Airplane acceleration	Accelerometer
		Electrical frequency	Frequency meter
		Airplane forward speed	Air-speed indicator
Potential		Electric potential	Voltmeter
Displacement	In space	Oil quantity, Fuel quantity	Liquid-level gauge
		Wheel position, Flap position, Oil-cooler flap position, Cowl flap position	Position indicator
		Level flight	Gyro-flight indicator
		Direction	Directional gyro, compass
		Weight	Altimeter
		Change in height	Rate-of-climb indicator
		Pitch angle	Inclinometer
		Stall conditions	Stall indicator
	In time	Drift	Drift meter
		Time	Clock

Power Plants

As the size of aircraft engines has increased, the necessity for improved control has been noted. Carburetor air temperature requires a sensitive measuring device capable of following quickly any change in temperature. The resistance bulb and ratio-type temperature indicator fills this need. Improved cylinder-head temperature indicators are being developed to permit engines to be operated at higher efficiency. The position of cowl flaps and oil-cooler flaps is indicated by electric instruments.

The very high temperatures encountered in jet-propulsion engines are being measured by thermocouples which constitute the only practical means.

Longer flights dictate rigid maintenance to flight schedules and the attainment of high efficiency in operation. Fuel flowmeters, utilizing electrical transmission, are standard equipment. New and improved horsepower indicators[4] aid in obtaining efficient engine performance.

Flight Conditions

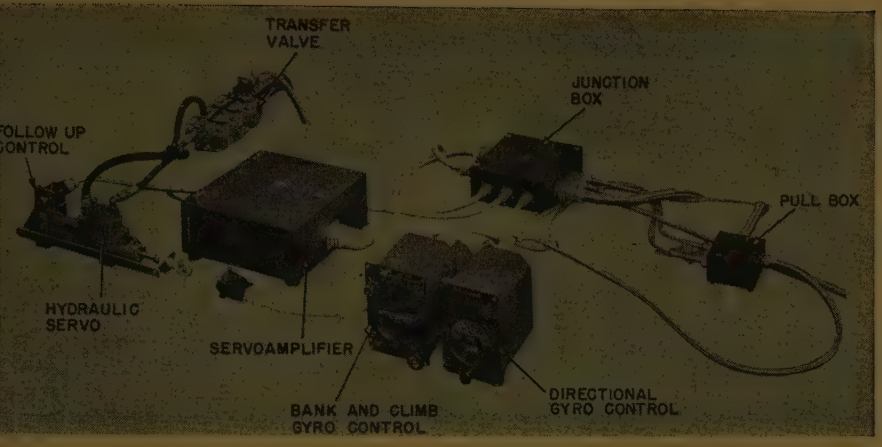
Airplane performance has been improved, so that flights are made under extreme conditions of weather, altitude, and temperature. Electric instruments aid by providing not only good performance in general under these conditions, but also provide some specialized indications. Rate-of-ice-accumulation instruments are electronically operated. Pressurized cabins for high-altitude flight present difficult problems in that the tubing and other connections for mechanically operated instruments must be sealed at the point of cabin entry. Airtight electric connections are made easily by gasketed lead-in studs.

Limited Methods

Certain measurements are made by one type of instrument because no other method appears practical. Clocks will

Table II. Operating Principles of Aircraft Instruments

	Self-Contained				Remote Indicating				
Class—Instrument	Electrical	Mechanical	Mechanical-Electrical	Mechanical-Pneumatic	Electrical	Mechanical	Mechanical-Electrical	Mechanical-Hydraulic	Mechanical-Pneumatic
Flight Instruments									
Altimeter		Aneroid							
Rate of climb		Manometer							
Air speed									Manometer
Free-air temperature					Resistance measurement			Pressure gauge	
Accelerometer		Mass measurement							
Inclinometer		Pendulum							
Flight indicator			Gyroscope	Gyroscope					
Rate of turn indicator			Gyroscope	Gyroscope					
Stall indicator			Pressure switch						
Navigational Instruments									
Directional gyroscope			Gyroscope	Gyroscope					
Compass		Compass			Magnetic saturation		Compass-magnetic saturation		
Clock		Escapement							
Driftmeter			Telescope						
Engine Instruments									
Pressure indicators							Pressure gauge—electric transmission	Pressure gauge—hydraulic transmission	
Temperature indicators		Bimetal			Resistance measurement and thermocouples			Pressure gauge	
Tachometer					Magnetic-drag electric transmission	Magnetic-drag machine coupling			
Synchroscope					Frequency comparison				
Fuel and oil-quantity gauge		Sight-gauge and float type					Float level—electric transmission	Float level—hydraulic transmission	
Flowmeter		Volumetric pump					Volumetric pump; Pressure measurement; Velocity measurement (Electric transmission)		
Horsepower indicator							Combined torque and speed indicator		
Exhaust—gas analyzer					Thermal conductivity—resistance measurement				
Miscellaneous Instruments									
Position indicators					Resistance measurement				
Ammeter	Direct current—permanent-magnet moving coil								
Voltmeters	Alternating current—moving-iron vane								
Wattmeters	Electrodynamic								
Frequency meters	Series resonance								
Ice accumulation					Capacitance measurement				



ure 3. Components of an electric–hydraulic automatic pilot

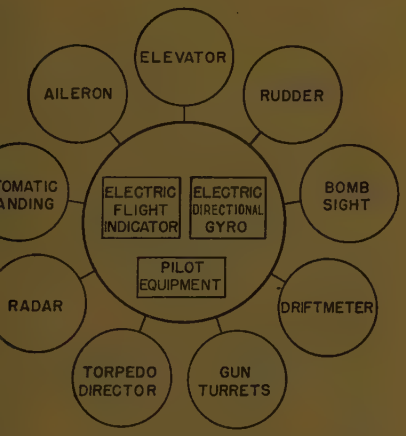
itinue to be mechanical until better ulated electric power sources are avail-e. Exhaust-gas analyzers and distant ding compasses have been mentioned viously. Radio altimeters are the only truments available for obtaining ab-ute height above ground. It is ex-ted that instruments operating en-ly on electrical principles will assist in aining better fuel quantity measure-nts.

SOCIATED EQUIPMENT

Electric instrumentation is easily apted to meet requirements calling for combination of two or more measure-nts or of measurement and control ctions. The horsepower indicator nbines engine torque and speed. The ctric tachometer and synchroscope rk as a team. Electric compasses are d to control the precession of direc-nal gyroscopes. Moreover, the remote alization of several measurements can be effected easily to determine, for example, the total quantity of fuel in a number of separate tanks.[5]

Electric gyroscopes, as used in automatic pilots, are outstanding examples of combined instrumentation and control. Figure 3 illustrates the components of an electric–hydraulic automatic pilot. The electrically operated gyro instruments[6] incorporate electric pickoffs which give the required signal for automatic pilot control. The operation of the flight indicator and the directional gyroscope as indicating instruments is not impaired, since the signal power required is negligible. Electronic amplifiers provide means for obtaining any power step-up required. Figure 4 identifies the functions which can be controlled by electric pilot instruments.

Other examples of associated instrumentation and control are overspeed control of turbosuperchargers which utilizes the tachometer generator, combining positioning motors and position transmitters, and automatic propeller synchronization.



ure 4. Possible airplane functions controlled by electric automatic pilots

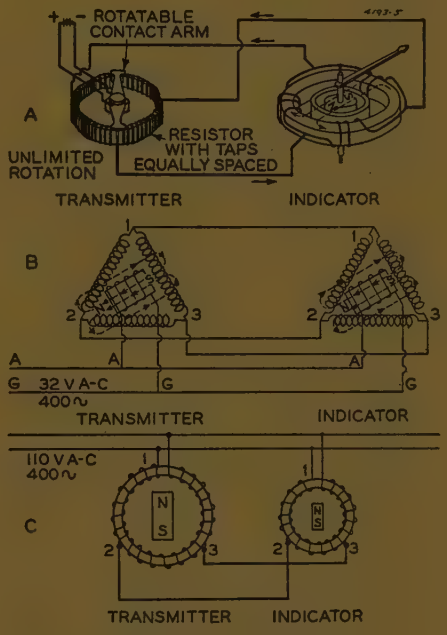


Figure 5. Typical circuit

A. D-c remote indication
B. A-c remote indication
C. A-c remote indication—induced second-harmonic system

ADAPTABILITY TO POWER SOURCE

Many instruments are available in forms to operate from d-c or a-c power sources. In addition, inverters, converters, and rectifiers can be used so that electrical instruments are applicable regardless of the electric system of the airplane.[7] Figure 5 illustrates circuits commonly used for remote transmission of aircraft measurements.

Conclusion

A review of the common aircraft instruments indicates that many operate only on electrical principles and that electric transmission for remote indication has been applied extensively to others. A few instruments cannot be improved by the use of electricity because of their inherent principles of operation or method of application, and these, no doubt, will continue to be mechanical.

Many factors have influenced the electrification of aircraft instruments. Because of the work done, the instrument needs of the aircraft manufacturer have been met, and many new instruments having superior characteristics have been developed. It is anticipated that technical improvement in aircraft instrumentation will continue, and that the benefits obtained from the use of electricity in this field will increase.

References

1. INSTRUMENTS AND MEASUREMENTS. *Power Plant Engineering*, January 1935, pages 8–11.

2. MAGNESYN REMOTE READING COMPASS, D. W. Smith. *Aeronautical Engineering Review*, volume 2, May 1943, pages 31–5.

3. ELECTRIC CIRCUITS AND THE MAGNETIC COMPASS, R. C. Burt, H. R. Beck. AIEE TRANSACTIONS, volume 63, 1944, January section, pages 24–6.

4. HORSEPOWER INDICATOR. *Instruments*, volume 17, April 1944, page 246.

5. TOTALIZING CONTENTS OF AIRCRAFT FUEL TANKS, J. R. Macintyre. AIEE TRANSACTIONS, volume 63, 1944, September section, pages 663–5.

6. ELECTRICALLY DRIVEN GYROSCOPES, H. M. Witherow, A. Hansen, Jr. *Aeronautical Engineering Review*, volume 3, March 1944, pages 61–5 and 147.

7. ALTERNATING CURRENT VERSUS DIRECT CURRENT IN HIGH-ALTITUDE AIRCRAFT, R. W. Gemmel, J. C. Cunningham. *Aviation*, volume 43, March 1944, pages 138–41 and 344–8.

Brief Survey of Power-Supply Developments on British Aircraft

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THIS paper covers the development in broad outline of electric generating equipment and systems on British aircraft from the 1920's up to the present time. The coverage, of necessity, must be very sketchy, but it is hoped that outlining British development in its approximate chronological order may be helpful in comparison with American development over the same period. No attempt will be made to go into great detail, and so pictures and curves have been omitted.

D-C Power Supplies

Early airplanes, which were mostly of the biplane type, used exclusively the so-called 12-volt d-c system. Power was supplied by a "windmill"-driven generator of 14 volts 500 watts capacity. "Windmill" is the British word for the wooden or metal propeller which was actuated by the slip stream of the aircraft, thus rotating the generator armature at its rated speed of approximately 5,000 rpm at the normal cruising speed of the airplane. Various "windmills" of differing pitch were available to take care of the various aircraft which had naturally differing cruising speeds. The generator was built in streamlined form and was mounted usually on the wing. Connection was made at the rear end by a plug and socket, and the weight of the complete assembly was about 20 pounds. Cooling was, of course, by the slip stream and care had to be exercised not to load up the generator to its maximum capacity when running up on the ground for test purposes; 40 per cent full load was the specified safe limit under these conditions unless an air blast was available.

Voltage control of these generators was achieved by a vibrating reed or Tirrill regulator, and also in the same box was located the reverse-current relay. It is worthy of note that these early regulators were screened in a metal box to minimize radio interference and the connecting cable was screened for the same purpose. Round about 1935, research and development were accelerated for obvious reasons which need not be discussed here, and about that time the airplanes which were destined to be our mainstays during the early part of the war were in various stages of development or production. These were the Spitfire and Hurricane fighters and the Whitley, Wellington, Hampden, and Blenheim bombers. All of these were fitted with a new type of engine-driven generator with self-regulating characteristics, which had been developed. The adoption of this generator, by rendering an external voltage regulator unnecessary, solved troubles which had been experienced with Tirrill regulators, the most bothersome of which had been radio interference.

It may be of interest to describe this generator in some detail. Three field windings were provided, a shunt winding for the primary excitation, a series winding to compensate for voltage drop in the generator due to load and thirdly, a control winding. This latter winding was arranged to carry battery-charge current only, and the resultant field was in such a direction as to check rising voltage with increasing engine speed, by the tendency to increase charge current. The principle worked out reasonably well in practice, but, like all excursions into new regions, it created its own peculiar problems which will be noted later. Cooling of these units was by air blast, the whole of the generator yoke being surrounded by an airspace. Power requirements also had increased, so that these generators were made available in 500 and 1,000 watt sizes at both 14 and 28 volts and weighing 22 pounds and 36 pounds, respectively. The speed range for rated full load was 3,000 to 5,500 rpm. It was during this period that the 24-volt system was tending to oust the 12-volt.

Experience with these generators under combat conditions during the early phases of the war revealed the limitations governing their performance. The battery-charge current varied between zero and approximately ten amperes for voltage control and, in general, bore no relation to the state of charge of the battery itself. Thus, if a battery were discharged by ground tests and repeated engine starting, the battery could not accept a heavy recharging current during flight, and undercharged batteries, useless in a crisis, resulted. Further a fixed resistance, switched into the field circuit by the engineer when the voltage reached 14.5 volts, had been found necessary, but this needed vigilance and was therefore undesirable. Another undesirable feature was that a resistance had to be substituted for the battery when the latter was removed from the airplane otherwise the generator was uncontrolled if the engine was run up. Lastly the voltage characteristics were not stable enough to allow paralleling, and this last problem rapidly was becoming important.

The power-supply policy therefore swung back to shunt-wound generators with external voltage control, and the movement was accelerated by the development of carbon-pile voltage regulators. The generators were built primarily for 24-volt systems but 14-volt units were made available in the 500-watt size for incorporation in later Spitfire and Hurricane aircraft. The speed range remained the same, and it is worth noting here that differing speed range, mounting pads, and drive shafts are the chief stumbling blocks to standardization between American and British units. A new unit of 1,500-watt capacity was added to the range, this being entirely due to improvements to the existing 1,000-watt model.

A brief history of British development of the carbon-pile voltage regulator for aircraft use may be of interest here. The principle is, of course, not new. The first model to go into production differed widely from the type now in general use and in some aspects this early regulator possessed definite advantages over the now standard model. The pile was exposed and not enclosed in a ceramic tube and the compression on the carbon stack was effected through a link system giving longer travels which were more easily controllable. The magnet system consisted of a specially shaped armature rotating on ball bearings between the magnet pole shoes, and a dashpot with a graphite piston was provided for damping to prevent hunting. Temperature compensation was provided by an Invar rod. It should be noted that on all British regulators current control is incorporated to provide a voltage characteristic falling from 28.5 volts to 26 volts with increase of load. These early regulators were comparatively expensive to produce, and a simpler type having shown promise, the former was gradually superseded by the latter. This is the Newton type, and it is substantially the same as the regulators which are being produced in quantity here in America, and so it is not proposed to describe this in detail here.

The equipment described in the last two paragraphs is that with which most of the current British aircraft are equipped, and it is proposed now briefly present developments in the d-c field; the position regarding a.c. development will be treated separately and discussed later. Two generators will soon be in production, one of 3,000 watts capacity and the second of 6,000 watts. By a curious coincidence the

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types *O* and *P*, and thus they corrend in output to American units of ilar nomenclature. The weights are pounds and 55 pounds respectively, but s believed that an improved 3,000-watt del is under development weighing 35 nds. These figures compare unfavory with the American designs, but it st be remembered that the speed ge remains 3,000 to 5,500 rpm, and o that the rated output is given conuously.

Voltage regulators have been imved in detail, and the most important nge has been to provide two carbon cks in series in the one regulator, one voltage control and the other to act a current limiter instead of the voltage and current coil operating on one stack. single-generator installations the voltcontrol keeps the voltage constant up the rated output of the machine, and n the current limiter comes into operan to drop the voltage sharply. In multiine aircraft where generators are alleled, each regulator gives a falling tage characteristic with load until load on each generator is reached, er which the current limiters act as ore. In addition the load current is en through a small coil acting on the tage pile to reduce the voltage of the erator if it tends to take more than share of the combined load.

Experiment and research are going ward with the paralleling of four of se larger 6,000-watt units in order to stable installations. The scheme wing most promise incorporates a h master regulator which is actuated the bus-bar voltage to keep this subntially constant, irrespective of the tage tendencies of the individual gentors feeding the system. As already plained, each generator is controlled its regulator to give a falling voltage racteristic with load, and also comsation is provided for equal load dision. In addition a tapping is taken m the swamping resistance in series h the voltage coil of each regulator, ew volts up from the negative bus-bar l. These four tappings are strapped ether, and the carbon stack of the ster regulator is placed between this mmon point and negative, that is, in allel with the sections of the swamping istances. The pile is actuated by a l across the positive and negative bus s, and is arranged to open with falling l close with rising voltage. The comation thus gives substantially connt bus-bar voltage, even though the tages of the generators individually tending to fall with load.

dio and Radar Power Supplies

At about the same time that d-c supes were being obtained from a "windl"-driven generator, radio power was also derived from a similarly actuated source. This streamlined machine was built to give 0.1 ampere at 1,200 volts and four amperes at ten volts, weighed 20 pounds, and was designed to run at a constant speed of 3,500 rpm. To give this last characteristic a slipping clutch, or a variable pitch "windmill," was used.

Following this, a range of motor generators was built which ran off the aircraft electric system. The outputs were in all cases 80 watts either at 1,200 volts or 600 volts, and the inputs were 12 volts or 24 volts to suit the aircraft main power supplies. These units weighed about 12 pounds and were approximately 4.5 inches in diameter by 11 inches long. An interesting point here is that a limit of 80 watts was placed on radio requirements from the aircraft battery, until the summer of 1939, when the demand suddenly was jumped to 750 watts.

Then came the war, and the urgent need for more and more power for radio and radar arose, and thus was born the engine-driven alternator, which to this day is the method most favored by the British for obtaining this power. The first model to go into production was a single-phase alternator rated 500 watts at 80 volts rms, and when fitted to British engines its speed range was 3,000 to 5,500 rpm. The frequency was 1,300 cycles per second at 3,000 rpm. Incidentally, it is not known by the writer what reasons governed the adoption by the British of 80 volts and the frequency range 1,200 to 2,400 cycles per second as the standards for these supplies.

The design of these alternators permits of a very robust mechanical unit. The field is energized by direct current from the aircraft's main d-c power supply, and no windings are placed on the rotating mass, the field coils being positioned on a conical-shaped projection of the end plate, which is bored to receive a cylindrical part of the rotor. The stator with the a-c windings is fixed to the yoke, and there are 24 teeth on the inside periphery. Aligned with the stator teeth are the rotor teeth, 26 in number, so that a pair of teeth coincide 26 times per revolution. The flux path is thus from the conical projection across the constant air gap to the cylindrical part of the rotor along the rotor to the teeth and across the air gap to the stator teeth. The magnetic circuit then is completed through the yoke. The flux is unidirectional but varies in magnitude because of the variation in reluctance of the magnetic path between rotor and stator teeth and the coincidence of these together with the speed determined the frequency.

Round about this time we were receiving Hudson aircraft built by the Lockheed Aircraft Corporation, and, of course, the aforementioned units were not suitable for the American engines powering those airplanes. A new unit of similar output therefore was produced with the necessary American standard flange and drive shaft. This gave full output at 2,000 rpm and upwards, the frequency at 2,000 rpm being 866 cycles per second.

However, not all aircraft are multi-engined and on single-engined airplanes no pads are available for the alternator if a d-c generator is fitted. To meet this situation, a range of dual machines was produced in which the d-c armature is on an extension of the rotor, and the one body carries the alternator stator and the d-c generator poles. Outputs were 500 watts alternating current plus 500 or 1,000 watts direct current, the variations being on the d-c side, the latter could be a plain shunt machine or one of the self-regulating type.

Recently two more alternators have made their appearance similar in construction to those already described but with increased outputs of 1,200 watts. One is suitable for British engines whereas the other is designed for American drives.

The latest design of dual machine is a combination of this 1,200-watt alternator and the 3,000-watt d-c generator. This unit incorporates all the improvements resulting from experience on previous generators. The d-c portion is a normal six-pole shunt machine with compensating windings, and the alternator half has already been described. Full output is obtained at a minimum speed of 3,000 rpm with air-blast cooling of 100 cubic feet per minute, the frequency at that speed being 1,200 cycles per second. The weight is 51 pounds.

Also under development are engine-driven alternators of 3,500 watts and 6,000 watts capacity, the weight of the former being about 35 pounds while that of the latter is unknown.

In the field of alternating current for aircraft power supplies, the only installation at the moment does not derive the power from an engine-driven alternator through a constant-speed device, but rather from auxiliary generating plants. The system is 110 volts, three phase, 250 cycles, but it is believed that this voltage and frequency have not been definitely frozen for future projects, and it is hoped that standardization with American practice may be achieved. Two units are installed in the aircraft, independent of one another, one being used while the other is a standby. The weight of each is 450 pounds complete. Each alternator is rated 20 kw at 3,750 rpm, and a d-c generator of three kilowatts at 28 volts and an exciter of 750 watts are included in each unit. Some development is going forward with constant-speed devices, an eddy-current coupling being one approach, but the weight of this device is excessive. Another approach is a slipping device with tapered rollers, but no information is available on the progress of this project.

Requirements for Low-Voltage Aircraft Cable

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Synopsis: This paper presents the basic requirements to be expected of any electric cable which is considered for use in aircraft. A few of the problems encountered in forming a cable specification are presented, along with factors to be considered in establishing valid test procedures. The author has not attempted to give solutions to the problems presented, but rather to acquaint the reader with the fact that a major problem does exist in designing an electric cable suitable for use in aircraft, and that many factors which are extremely variable must be considered before an adequate solution can be found.

ELECTRIC wiring designed for aircraft probably is required to withstand a wider range of operating conditions than most other types of electric wiring. This is not surprising since present airplanes, both combat and commercial types, are expected to be able to fly anywhere in the world under all kinds of weather conditions. Repair of a damaged airplane in remote parts of the world must be accomplished with a minimum of replacement parts, and probably only basic tools, such as screw driver, pliers, hammer, and wrench. In many cases repairs can be made only by salvaging parts from other airplanes. If the original electric wiring of an airplane is properly designed, installed, and protected against damage due to overloads, short circuits, insulation failure, abrasion, fluids, and fungus growth, and so forth, a great deal of wire replacement and maintenance will have been eliminated in both combat and commercial types of aircraft, although in combat airplanes the larger percentage of wire replacement is caused by gunfire damage.

The following basic requirements are to be expected of any electric wiring considered for use in aircraft:

1. *Minimum Weight.* Unnecessary weight is expensive in an airplane because it results in a reduction of the performance characteristics and the range. A one-pound increase in the weight of an airplane when empty easily may result in three pounds increase in the gross weight because of the weight of additional structure and fuel needed to carry it. This shows that a considerable reduction of "pay load" results when additional structure and fuel are utilized to carry unnecessary equipment weight.

2. *Minimum Diameter.* Excessive wire diameter reflects itself in extra weight. Bundles of wire with excessive diameter will necessitate larger clips to hold them in place. Routing enlarged bundles through the structure may mean an increase in the size of openings through stressed portions of the airplane and result in weakening the structure unless an additional thickness of metal is used around the openings which have been enlarged. The additional strengthening material means added weight. If the diameter of the bare stranded conductor is larger than necessary, the diameter of the shank of the terminal lugs will have to be increased to accommodate the enlarged wire. An oversize in conductor diameter means that more insulation material and braid will be necessary to cover the wire. The size of the "insulation-grip" type of terminal, sometimes used on smaller-gauge wires, also will be increased. With the realization that most airplanes carry several thousand feet of electric wiring, and some of the larger transport airplanes carry several miles of electric wiring, it is easy to see that a small increase in size in the individual conductor can result in an appreciable increase in the weight of the finished airplane.

3. *Noninflammability.* The insulation on electric cable can be ignited by either a sustained short circuit or a flame external to the wire. If the insulation is not self-extinguishing, the burning wire becomes extremely hazardous, since it may propagate the flame from one point in the airplane to another, and it can ignite other inflammable materials and be the cause of a major fire. This dangerous situation has been intensified in the present-day combat and cargo types of airplane, because of the use of open wiring throughout the airplane. The insulation on wiring should be self-extinguishing.

4. *Wide Temperature Limits.* Wire and its insulation should show no signs of failure when used in ambient temperatures between +125 and −70 degrees centigrade. A military airplane used in Arctic regions today may be transferred to equatorial regions next week. For maintenance the wire must be capable of being bent and flexed at all temperatures without damage to the primary insulation. For operation the wire must be capable of standing the ovenlike heat in the engine section of an airplane which has just returned from a flight and is allowed to stand in a blazing tropical sunlight while the engine "cools off."

5. *Abrasion Resistance.* The braid covering of an aircraft wire must be capable of withstanding a great deal of abuse in installation and service. During installation and assembly, wiring is subjected to being pulled through small openings and across sharp metal edges, and to general rough shop treatment. In service-vibration and airplane motion may cause the loosely bundled open wiring to beat itself against a piece of structure or chafe against the side of an opening through which the wire passes.

6. *Fluid Resistance.* Engine oil, hydraulic fluid, high-octane gasoline, salt water, alcohol, ethylene glycol, and any other fluid likely to be encountered should have no effect upon the outer protective coating of the wire. Open wiring in the engine nacelles, wheel wells, and other exposed sections is subject to all of the aforementioned fluids during operation and maintenance and therefore should not be materially damaged by exposure to any or all of them.

7. *Fungus Resistance.* This property is most important for airplanes used in tropical areas where high heat can be accompanied by high humidity. Wiring inside equipment and in confined parts of the fuselage may be attacked by fungus which will cause destruction of wire insulation and require early replacement.

8. *Maximum Current Capacity.* In a 28.5-volt d-c electric system the maximum current-carrying capacity is limited in almost all cases by the maximum allowable line-voltage drop rather than the rating of the wire. The allowable line-voltage drop has been set by specification and is dependent upon the operating requirements of the equipment. In the larger airplanes almost every circuit is carrying only a fraction of the current it is capable of handling because of the voltage-drop limit. A few circuits do carry their rated current, but in these isolated cases the length of wire between the main bus and the apparatus is very short and the wire capacity can be utilized without exceeding the voltage-drop limit.

9. *High Dielectric Strength.* Within the range of operation of present-day aircraft pressure altitude and relative humidity changes have no measurable effect upon the dielectric strength of wiring insulation. Pressure altitudes varying from sea level to 50,000 feet, and relative humidity from 0 to 100 per cent are encountered. The test voltage of from 2,000 to 5,000 volts which the finished cable is required to withstand when passing through a chain-electrode spark-test machine is for the purpose of discovering flaws in the insulation material such as a thin-walled section of insulation, a small chip of metal in the material, an air bubble, or any other flaw which might cause mechanical or electrical insulation failure during installation and service. Low test voltages of a magnitude approximating those maximums that actually may be encountered would not be sufficient for revealing hazardous insulation flaws.

Establishment of a Specification for Aircraft Cable

In order that the wire manufacturer might have a requirement basis upon which the design and construction of an acceptable aircraft cable could be based

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npts have been made to formulate a ification for low-voltage aircraft elec- cable. To aid in the determination of ecification suitable to the Army and y, the aircraft industry, and the wire ufacturers, the Technical Advisory mittee on Aircraft Cable (TACAC) been established, whose membership ides representatives of these organiza- s. A specification acceptable to all erned has been difficult to achieve, e each group has different problems ideas concerning the same subject. present specification resulting from nsive investigation is an Army–Navy onautical Specification *AN-J-C-48*.

Performance Specification. This type pecification presents the performance irements to be met by a manufacturer's uct. Specific test procedures are pre- ed which are considered valid tests for rmining whether or not a product is ptable. Within certain limits, deter- ed by production limitations, design re- ements, and specific uses, the manufac- r has considerable freedom in the design construction of electric cable. Any able material may be used, provided the ormance requirements are met. This is most desirable type of specification if the mum requirements are set high enough, e it encourages the manufacturer to de- p new materials and methods of manu- ure.

Manufacturing Specification. This of specification is the most rigid of all, e it specifies how a product is to be de- ed, what materials are to be used, and t manufacturing process is to be used. little deviation by the manufacturer is ated. This type of specification should voided unless absolutely necessary, since es not encourage design development on part of the manufacturer.

ery few specifications can be classified rely as one of the preceding types, e most of them are a combination of types. Wire specifications are nly of the performance type. In some ances, however, desired wire char- ristics can be attained only by use of known material or manufacturing ess, and therefore use of that material rocess must be made mandatory. In r instances, several materials ca- e of yielding the desired characteris- if properly used, may be known, but ay not be feasible to set up a test pro- ure which will validly compare the tive merits of the several known ma- ls. In this case, the use of a par- ar material may be made mandatory l a test procedure is established which weigh the relative merits of the sev- materials.

ablishment of Valid Test rocedures

writing a specification for low-volt- aircraft wire, stating the qualities a wire should have in order to meet specification is a relatively easy mat- ter. The most difficult part is the establishment of test procedures which will give a valid measure of whether or not a particular type of wire will have the desired qualities and will be satisfactory for use in aircraft. For every type of qualification test, a large number of variable factors must be considered. Controlled laboratory tests and procedures should be designed to reproduce conditions to which the wires will be subjected during installation and service.

1. To establish a valid test procedure for determination of the flammability of wire the following factors are important:

(*a*). Wiring position is an uncontrolled variable, but is an important variable insofar as fire and flame rate of travel is concerned. Wire insulation will burn more rapidly, and flame will travel more quickly when the wire is held in a vertical position, or at some angle of slope than it will when held in a horizontal position, because the fire will tend to preheat the insulation above the point which is actually burning, and therefore aid in propagating the flame. A large portion of aircraft open wiring is in some position other than horizontal. Because wiring offers a means for carrying flame from one point to another, the flame rate of travel for the worst condition of wire position should be very small. The insulation should be self-extinguishing.

(*b*). Wire size is important in establishing a flammability test. A small wire will burn more readily than will a large wire. The insulation on an *AN*-0 wire will be more difficult to ignite than will the insulation on an *AN*-18 wire, because of the larger mass of copper which will dissipate the heat more rapidly. An *AN*-0 wire may not continue burning, because of heat dissipation by conduction through the copper, but an *AN*-18 wire may sustain or propagate combustion for quite some time.

(*c*). Small wires are loosely bundled together in open wiring systems. If one wire burns, it may ignite others and cause more rapid spread of the fire.

2. In establishing an abrasion test for wire, it becomes difficult to establish limits for the variables, since each organization will have its own requirements regarding the type and amount of abrasion the wire will have to withstand during installation and service. Installation problems and procedures differ so much that an abrasion-resistant wire acceptable to one concern may not be acceptable to another. Different types of abrasion are encountered in installation and service, namely:

(*a*). A cutting type of abrasion, due possibly to a wire resting on the edge of a piece of metal and allowed to move in a direction which would cut the insulation and braid.

(*b*). A scraping type of abrasion, encountered during installation and maintenance when wiring is pulled through the airplane structure.

(*c*). A pounding type of abrasion, caused by airplane vibration which makes the wire beat itself against structure.

All types will be subjected to varying degrees of intensity. The speed of the scraping action, the sharpness of the cutting edge, the weight of the wire or wire bundle, the frequency of impact, among other variables all will vary between wide limits. The difficulty in setting up a procedure to test for all of these variables can be appreciated. The problem is complicated further by the use of different materials in wire manufacture. Comparing abrasion resistances of cotton-braided, glass-braided, and rayon-braided wires is complicated by the use of different impregnating materials and protective coatings. A rayon or glass braid, in general, is inferior to a cotton braid on the conventional scrape-type abrasion-test machine if the same type of protective coating is used. However, by use of the proper impregnating material or coating, a rayon- or glass-braided wire can be made to appear superior to cotton-braided wire on the same abrasion test machine, and yet inferior on another test apparatus which utilizes a different means for abrading the wire. A definite need is indicated for an abrasion test, and suitable apparatus which will compare truthfully the different types of wire on a basis which will account for variables encountered in usage and will give consistent results regardless of material used.

To enumerate the factors affecting all types of specification tests would take considerable more space and time than has been allotted for the coverage of so vast a subject. It is sufficient to say that the problems peculiar to aircraft are many and varied, but are of the utmost importance in assuring a safe, dependable airplane, capable of operation in any part of the world with a minimum of maintenance.

Conclusions

By means of this paper, the author has attempted to show that a major problem of design has been given to the wire manufacturers by asking for an electric cable which will not fail in operation under conditions of:

1. Ambient temperatures from −7 +125 degrees centigrade.

2. Relative humidities from 0 to cent.

3. Pressure altitudes from sea 50,000 feet.

In addition, the cable is require noninflammable, abrasion-resistan resistant, and fungus-resistant, a still possess minimum weight, mi diameter, maximum current-carry pacity, and good dielectric qualiti similar problem also has been given committees and boards which dete the specification test procedures for ing a valid measurement of the previou stated requirements.

The wire manufacturers, the armed services, the working committees, the aircraft manufacturers, and all others concerned with the problem of design, manufacturer, and use of electric cable have done an excellent job in establishing and maintaining the quality of wire that is

Requirements for Aircraft Electric Motors

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DESIGN requirements for electric motors intended for use on aircraft may be classified roughly into performance and construction. These two classifications are not completely independent of one another, as some of the construction requirements will have pronounced effect on the performance characteristics. Performance characteristics may be subdivided into the following headings:

(*a*). Operating voltages.
(*b*). Torque requirements.
(*c*). Rpm limits.
(*d*). Duty cycle.
(*e*). Efficiency.
(*f*). Ambient-temperature conditions.
(*g*). Protection against overheating.
(*h*). Radio-noise output.

Construction requirements may be subdivided into the following headings:

(*a*). Resistance to vibration and accelerations.
(*b*). Corrosion protection, sand and dust resistance.
(*c*). Gearboxes, clutches, and brakes.
(*d*). Types of bearings and lubrication.
(*e*). Limit switches.
(*f*). Provision for emergency hand operation.
(*g*). Method of mounting and bringing out leads.
(*h*). Maximum over-all dimensions.
(*i*). Maximum allowable weight.
(*j*). Maintenance considerations.

The manner in which the preceding requirements are established and the specific reasons for some of the requirements will be outlined briefly in the following paragraphs with particular reference to low-voltage d-c motor applications.

ting Voltages

al operating voltage at the motor ered to be 27 volts for continuous- tors and 26 volts for intermittent- otors. In case of failure of the enerators, the voltage under any iable load may fall as low as 20 nd satisfactory emergency operation some motors may be specified at w voltage. Improper setting or nctioning of the voltage regulators permit the line voltage to rise as high 0 volts occasionally, and the over- eding thus created must not impair operation nor running balance of the motor. All parts of the motor must be designed to absorb the torque and momentum loads imposed by sudden stoppage of the armature due to jamming of the driven load at the highest service voltage without damage or loosening of parts.

Torque Requirements

For unlimited rotation applications the starting torque required is usually determined by the type of load to which the motor is connected. Motors driving hydraulic pumps and similar accessories are usually required to supply full-load torque at starting. Motors driving fans, blowers, generators, and similar accessories normally require only a fraction of full-load torque for starting. To provide a margin of safety for the higher frictions encountered at low temperatures, shunt- and compound-wound motors should have a minimum starting torque of 175 per cent normal running torque, and series-wound motors should have a minimum starting torque of 300 per cent normal running torque.[1] Normal running torque is defined as the torque corresponding to the horsepower and the rpm appearing on the nameplate.

Many aircraft motors have a load which is not constant, so that continuous torque ratings are meaningless. This is particularly true for linkages employing toggles where the force required increases rapidly at the overcenter position and then decreases. The first step in applications of this type is a thorough study of the load to be operated, by plotting the torque required versus either time or the percentage of operating cycle. Total torque which the motor will be required to deliver at any time consists of the weight loads for ground operation plus maximum air loads due to flight plus any additional loading due to ice accumulations on exposed surfaces, and increased friction due to cold grease, dust, and differential contraction of metallic members.

With the loading curve in Figure 1 the values of maximum torque required and the points at which they occur may be determined. By introducing the maximum time permissible for completion of the cycle and the number of cycles required per hour, the duty cycle and the intermittent horsepower rating required can be established. If a fixed percentage of the cycle need not be completed per unit of time, the varying speed characteristics of a series-wound motor may be used to obtain a reduction in motor weight and to simplify the control required.

Rpm Limits

In the interest of weight reduction, the motor speed must be as high as possible. The upper limit of continuous speed at present is about 12,000 rpm, this limit being imposed by satisfactory ball-bearing operating and life. On larger sizes of d-c motors, the upper limit of speed may be established by the danger of the armature windings and commutator bars being thrown out of position by centrifugal force.

Duty Cycle

Duty cycle is usually specified as so many minutes "on" followed by a certain number of minutes "off." Given the number of complete operating cycles required per hour and using Figure 1 as a basis, the duty cycle of the motor can be established, enabling selection of the lightest motor which will power the application without dangerous overheating. In calculating the duty cycle, allowance must be made for those applications where a large number of short runs, that is, "inching" toward the desired setting, are common, as in wing-flap or trim-tab positioning service. Applications of this kind impose a much more severe duty cycle on the motor than running continuously from start to finish of the operating cycle. Open-type self-ventilated motors save weight for continuous-duty applications, but provide little or no weight savings on highly intermittent-duty applications. In general, an intermittent-duty motor is lighter than a continuous-duty motor of the same rating.

Efficiency

Minimum allowable efficiencies for 24-volt d-c motors from 0.02 horsepower to 4.0 horsepower are specified in Army–Navy specification *AN-M*-10.[1] While these values are intended for military aircraft and may, at first analysis, appear to be unnecessarily low, they represent a sacrifice of efficiency to obtain a lower motor weight. Higher efficiency could be obtained by increasing the amount of copper and iron in the motor

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being used in present-day aircraft. Their efforts are rewarded by the thousands of airplanes which fly daily with a minimum of failure in the electric systems due to causes other than gunfire. Many important problems, however, are yet to be solved, and their ultimate solution will depend mainly on the continued and coordinated effort of all organizations concerned.

but usually cannot be justified, because in addition to the weight of the motor, the weight of the extra gasoline required to carry the heavier motor also must be charged against the more efficient design. High efficiency is of much more importance in continously operated motors than in intermittently operated motors.

Ambient Conditions

Operating temperatures for motors mounted in exposed locations on the airplane range from −65 degrees centigrade to +70 degrees centigrade,[1] and this complete range may be encountered within an hour. Location in cabins or other semiprotected locations does not necessarily mean more favorable ambient temperatures, since airplanes are frequently stationed at operating bases where hangers are not available.

Ambient atmospheric pressures vary from 14.7 pounds per square inch at sea level to 2.72 pounds per square inch at 40,000-feet altitude. This reduction in pressure, although slightly offset by the reduction in temperature as the altitude increases, results in decreased weight of air per cubic foot and forced cooling based on a definite number of cubic feet of air per minute will be inadequate unless the necessary allowance is made in calculating cooling air available. Motors located in supercharged compartments ordinarily will not be subject to ambient pressures lower than 9.5 pounds per square inch. Creepage distances satisfactory at sea level must be increased 300 per cent for satisfactory operation at 40,000 feet.

Ambient humidities will vary from 0 to 100 per cent. Service reports from the South Pacific areas disclose that the humid warm climate is particularly conducive to the creation and rapid development of fungus growth on textiles and all other organic materials, including those used for electrical insulation, unless prevented by a fungi retardant included in the insulating varnish or applied over the varnish. Under ideal conditions for growth, enough fungi will develop in one week to fill completely the air gap between armature and pole shoes and prevent the armature from turning.

Motors located in wings or other compartments where explosive mixture of gasoline vapor and air may accumulate must be provided with suitable explosion-proof housings and have excellent electrical bonding between all parts of the motor enclosure and the supporting structure to eliminate any possibility of igniting such mixtures by an electrical discharge between the motor and structure. The resistance between any two metallic components of the motor and between the motor base and the airplane structure must not exceed 0.002 ohm.

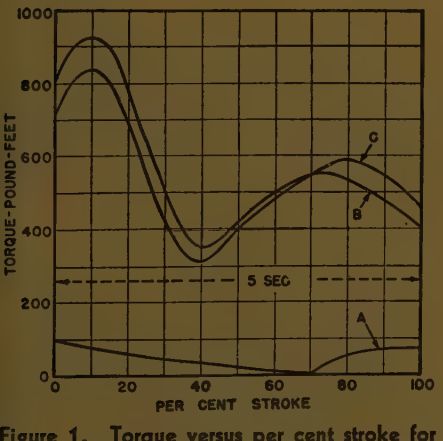


Figure 1. Torque versus per cent stroke for typical aircraft application

A shows torque required for weight of door
B shows torque required to overcome air loads on door
C shows total torque required from the motor

Protection Against Overheating

The question of whether or not thermal protective devices should be required on all motors is very controversial at the present time. For critical circuits, such as propeller feathering, it is obviously unwise to include a "protective" device which will prevent operation of the motor just because it is five degrees above the established safe temperature of the motor windings when unrestricted operation of the feathering pump motor, even though it be severely damaged in the process, would eliminate a dangerous flight condition. On other motors not supplying "time-critical" functions, such as transfer pumps and cabin superchargers, built-in thermal switches protect the motor against unintentional or unsuspected overloads by cutting the motor off the line until it has cooled to a safe temperature. The cycling of the motor due to automatic reclosing of the protective device gives warning to the crew that something is wrong with either the motor or its load, and they may then either clear the trouble in flight or cut the circuit off the line for repair at the next ground stop.

Radio-Noise Output

Greatly increased amount of radio equipment now being used on military airplanes, together with the greater receiver sensitivity and nonshielded wiring installations, have made minimizing of radio-noise voltages imperative. Radio-noise voltages may be generated by d-c motors because of poor commutation, poor combination of stator and rotor slots, and straight slot rotors. The high-frequency voltage ripples may travel either by direct radiation or by conduction along the primary power lines to emerge eventually as an unbearable noise in the radio-receiver headphones. In addition to poor commutation due to improper design, poor seating of the brushes on the commutator has proved a prolific source of radio noise. On a one-eighth-horsepower 1,750-rpm 24-volt-d-c pump motor, for example, the radio-noise voltages measured as shown in Table I.

Table I

Frequency (Kilocycles)	Motor Load	Microvolts on Motor Leads	Radiation Noise at Four-Foot Distance
200	No load	45,000	1.5
200	50 per cent	200,000	1.0
200	Full load	200,000	1.6
600	No load	20,000	12
600	50 per cent	30,000	16
600	Full load	25,000	22
1,000	No load	8,000	1.0
1,000	50 per cent	13,000	1.3
1,000	Full load	21,000	2.0
10,000	No load	70	1.0
10,000	50 per cent	80	1.1
10,000	Full load	100	1.2
20,000	No load	10	1.1
20,000	50 per cent	12	1.2
20,000	Full load	13	1.2

On disassembly the brushes were found to have only 25 per cent area of contact with the commutator. After seating the brushes properly and cleaning the smut from the commutator surface, the radio-noise voltage dropped to 50 microvolts at 200 kilocycles per second and correspondingly less at all the higher frequencies. The radiated-noise field disappeared completely after this treatment.

From a weight standpoint, it is not efficient to apply noise filters to all rotating electric equipment, and, therefore, it becomes necessary to minimize the noise each item of equipment produces and to supply a power-line filter just ahead of the radio receivers.

Resistance to Vibration and Acceleration

Although the terms are often used interchangeably, acceleration and vibration are not the same. Vibration always gives rise to acceleration, but acceleration can exist without vibration. The general formula for acceleration is:

$$\frac{a}{g} = \frac{(2\pi f)^2 r}{386}$$

where:

$\frac{a}{g}$ = number of g's acceleration in feet per second per second
f = frequency in cycles per second
r = peak amplitude in inches (one-half the total excursion)

Values of acceleration and vibration vary widely from one type of airplane to another, and even in different sections of the same airplane. The problem is further aggravated by motor mountings which may be flexible enough to permit resonant vibration, in which case the acceleration may increase as much as 20 times. If resonance is not present,

ability to withstand accelerations of 12g applied at the center of gravity in any direction generally will be satisfactory for aircraft applications, including the acceleration value obtained by landing impact.

Table II indicates the range of vibration and acceleration encountered on a typical twin-engine medium bomber.

Corrosion Protection, Sand and Dust Resistance

Use of light metals, such as aluminum and magnesium, to reduce weight has aggravated the corrosion problem, since steel, copper and other metals lower in the galvanic series in sea water also must be used in the motor, often in intimate contact. Effective and durable protective finishes must be provided between dissimilar metals; otherwise any condensation or salt spray will form an electrolyte, and the aluminum and magnesium will be destroyed rapidly by galvanic action. Severe condensation occurs when a descent is made from the cold air of high altitudes into the warmer air near the ground.

Global operation subjects motors to fine sand and dirt Given sufficient time, this sand eventually will penetrate any seal except the hermetic type. These abrasive particles rapidly grind away bearings, brushes, commutators, switches, and gears once they have gained entrance. The nearest approach to a solution at present is to design as effective a seal as possible and to keep the seal effective by periodic cleaning or replacement.

Gearboxes, Clutches, and Brakes

Gears for aircraft motor applications present a difficult design problem because of the small weight and space allowed for them and the difficulty of providing satisfactory lubrication over the wide range of temperatures encountered. Sintered iron gears impregnated with oil have given satisfactory service on fractional horsepower motors. Hardened steel gears with low-temperature grease have been fairly satisfactory for larger geared motors, but no low-temperature grease yet available is entirely satisfactory at high temperatures. Gears must be capable of absorbing the shock caused by the jamming of the driven mechanism at maximum motor voltage without damage or breakage.

Clutches are specified when the momentum of the armature must be removed from the load to prevent overshooting of the output shaft when the brake is applied, or when the frictional load of the armature and its associated gearing is to be removed from the system to facilitate hand-crank operation.

Bearings and Lubrication

Bearing sizes used on commercial motors are too small to withstand the loads and vibrations encountered in aircraft applications.

In general, ball bearings have proved more satisfactory in service than sleeve bearings, and sealed-type ball bearings are preferred, particularly for the higher speeds. Bearings must be designed to take thrust from both directions. However, because of procurement difficulties, sleeve bearings must be substituted for ball bearings when adequate supplies of the latter are not available. Oil-impregnated bearings have not proved too satisfactory. Excessive lubricant must be avoided. The type of lubricant must be such that it will provide lubrication at the extreme high and low temperatures encountered. The lubricant should not become watery at the high temperatures or go solid at the low temperatures. At present, lubricant in accordance with Army–Navy specification *AN-G-3*[2] is the most suitable for meeting these conditions. Enough lubricant should be provided for 500 hours of operation without attention, with the motor operating in any position.

Limit Switches

To provide the utmost reliability of operation, limit switches should be mounted on the mechanism being operated. In order to simplify wiring, however, limit switches are usually incorporated in the driving motor. Switches used for this purpose must be extremely resistant to vibration and must have adequate current and voltage-breaking capacity both at sea level and at high altitudes. Snap-action contacts are necessary and compactness is important. Means of adjusting the setting of the limit switches from outside the motor must be provided and the setting adjustment locked against changes due to vibration or continued operation of the motor.

Table II

Location	Total Excursion (Inches)	Frequency (Cycles Per Second)	Acceleration (Ft/Sec/Sec)	Maximum Acceleration at Resonance
Engine accessory case	0.020	50	2.56	
Engine accessory case	0.006	185	10.5	40
Fuselage nose structure	0.007	148	8.0	18
Fuselage cabin structure	0.002	148	2.0	40

Provision for Emergency Hand Operations

Some applications, such as landing-gear actuators, require that means for hand operation be provided in case of failure of the electric system for any reason. The hand-crank extension should not turn when the motor is driving the mechanism, nor should it be necessary to turn the motor armature when operating the mechanism with the hand crank. The torque required to turn the hand crank should be low enough so that one man can turn the crank continuously without requiring the advantage of his full weight. Each application of this kind requires individual consideration to establish the most suitable design.

Method of Mounting and Bringing Out Leads

Type of mounting is determined by the individual application. Flange mountings are lighter than base mountings, but are subject to high stresses due to bending moments, particularly if resonant vibration is encountered. Flange mountings must provide sufficient space to permit insertion and removal of bolts or nuts and adequate wrench clearance for tightening them. Tapped holes directly in aluminum are not satisfactory, and such applications should employ plated-steel inserts.

Leads for small motors not subject to severe vibration should consist of multiple pin receptacle in accordance with Army–Navy specification *AN-W-C-591*.[3] Leads for large motors where suitable *AN* connectors are not available, and for all motors on which severe vibration makes *AN* connectors unsatisfactory, terminal studs should be provided. Terminal studs must be anchored securely against turning. Leads inside motors must be anchored firmly and protected against abrasion.

Maximum Over-All Dimensions

Over-all dimensions must be as small as possible, since space is at a premium, and all space possible must be left around each unit to provide for inspection, adjustment, ventilation, and removal as necessary.

Maximum Allowable Weight

Every pound saved on the weight of a motor permits more than one-pound additional pay load to be carried by the airplane. The value of one-pound weight saving is variously estimated by commercial airlines at from $100 to $1,000 per year. In military airplanes no momentary value can be placed on the ability to carry a few more rounds of ammunition or extra gasoline, but the objective is obviously very important. Motor designs must not penalize reli-

Functional Design of Aircraft Electric Actuator Equipment

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IN their order of importance the factors which influence the design of an electric actuator for use on aircraft are:

A. Dependability under extreme operating conditions of:

1. Vibration.
2. Acceleration.
3. Temperature.
4. Altitude.
5. Humidity.

B. Performance of intended function with regard to:

1. Speed of operation.
2. Accuracy of movement.
3. Duty cycle.

C. Safety of operation is provided by:

1. Emergency operation provisions.
2. Avoidance of inadvertent operation.
3. Avoidance of fire hazard.

D. Weight influenced by:

1. General schematic arrangement.
2. Choice of materials.
3. Treatment of materials.
4. Detail design of parts.

E. Availability of component parts influenced by:

1. Importance of application.
2. Production schedule.

F. Cost as a function of:

1. First cost in money influenced by:
(*a*). Simplicity of design.
(*b*). Availability of materials, parts, and facilities.
(*c*). Quantity of units to be produced and production schedule.
2. Cost of maintaining the equipment:
(*a*). Money spent throughout life of the actuator.
(*b*). Flight time lost because of maintenance.
3. Cost in loss of performance due to weight.
4. Cost in loss of pay load due to weight.

The purpose of this paper is not to give detailed instructions, but only to point out to the designer of aircraft electric actuating equipment the points which must be considered in formulating such a design and to indicate the methods of obtaining solutions to the many problems involved. The design of equipment of this nature will be found to be about one-fourth electrical and three-fourth mechanical engineering and should only be attempted by an engineer or group of engineers with broad training and experience in both fields.

Electric actuators on aircraft are used to perform functions such as retracting and extending the landing gear, extending and retracting landing and maneuvering flaps, controlling engine cooling air flow, adjusting control surface trim tabs, handling cargo and bombs, and for many other services. From a study of these applications it is seen that the actuator must be dependable and give satisfactory operation under the conditions encountered in aircraft operation. The actuator will be subjected to vibrations at fundamental and harmonic frequencies to 200 cycles per second at amplitudes that produce accelerations to 20 gravity. Most actuators must be installed without shock insulating mounts and therefore of necessity will be rugged in construction with the electrical control balanced to avoid inadvertent operation of mechanism elements or electrical contacts. The actuator and its control should be suitably designed to operate in ambient temperatures of −65 to +160 degrees Fahrenheit at any altitude to the service ceiling of the aircraft and under all conditions of relative humidity to 100 per cent saturation. This will mean that particular attention must be paid to the selection of lubricants, oil seals, breathing provisions, and corrosion preventing treatments. At altitudes above 25,000 feet the brush life of d-c machines will be materially decreased, and it will be necessary to use especially treated brushes in units that are required to operate for any appreciable percentage of the time at these altitudes. Another point to be considered in high-altitude operations is that the breakdown voltage for a given air gap or creepage is very considerably lower than at sea level.[1] Some investigators have attempted to show that at extreme altitudes the cosmic rays or other radiations have an effect on the electrical performance of contacts and air gaps.

In order to design an electric actuator that will perform satisfactorily it will be necessary to have accurate data concerning the direction and magnitude of the forces required of the unit at all portions of its movement, the speeds required at all points, and the frequency in time at which this movement will be required. With this information and with some knowledge of the actuator mechanism, it will be possible tentatively to select a motor with the proper speed–torque characteristics and to design a suitable electric control system. The accuracy with which the actuator is required to position its mechanical output will be dictated by the type of application and by the mechanical means used to connect the actuator to its load. The most widely used device is the limit switch which is used in conjunction with an electric brake

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ability of operation to save weight, but if considerable weight can be saved by shortening the useful life without affecting the reliability during the shorter life, the saving in weight will justify a higher cost for the lighter motor.

Maintenance Considerations

Every airplane which is not in flight nor immediately available for flight is a liability instead of an asset. Ninety per cent of "the airplane not available" time on the ground is required for maintenance work, with poor weather accounting for the remaining time. Designs which facilitate inspection and maintenance obviously will increase airplane availability.

Of necessity, military maintenance and repairs often are performed at poorly equipped temporary bases by inexperienced personnel outfitted with only the simplest of tools. Consequently, aircraft electric motors must be as simple, rugged, and easy to assemble or disassemble as the designer's ingenuity can make it.

Design features which permit rapid and efficient maintenance of electric motors include the following:

(*a*). Lubrication points clearly marked.
(*b*). Brushes readily removable for inspection and replacement.
(*c*). Spare parts exactly interchangeable without requiring reworking.
(*d*). Army/Navy standard bolts, nuts, and screw threads throughout.
(*e*). Complete identification on a permanent nameplate.
(*f*). Readily removable inspection covers.
(*g*). External adjustments for limit switches, slip clutches, and so forth.

In addition, a complete set of service instructions should be supplied to the aircraft manufacturer, showing sectional views of the motor, detailed disassembly and reassembly procedure, servicing data, spare-parts catalogue, and so forth, so that all these data may be included in the *Erection and Maintenance Manual* supplied with each airplane.

References

1. Army–Navy Aeronautical Specification *AN-M-1D*. MOTORS, AIRCRAFT DIRECT CURRENT, 24 VOLTS, May 25, 1943.
2. Army–Navy Aeronautical Specification *AN-G-3a*. GREASE, LOW-TEMPERATURE LUBRICATING, March 6, 1944.
3. Army–Navy Aeronautical Specification *AN-W-C-591*. CONNECTORS, ELECTRICAL AIRCRAFT, December 19, 1941.

on the motor. This device gives fairly accurate positioning which may be further improved by adding dynamic braking or plugging provisions to the control system. If extreme accuracy that will not vary with load, voltage, or temperature is required, it will be necessary to build into the mechanical part of the actuator provisions for mechanically limiting the motion and to absorb the kinetic energy of the moving system. In some applications of electric actuators, it may be necessary to stop the motion or cause reversal when the load increases or decreases beyond specified limits. This effect may be conveniently accomplished by measuring the reaction on a stationary ring gear of a planetary system of gearing and thereby obtaining a force that is proportional to the amount of torque being applied to the system. This force may be opposed by springs and the movement thus obtained may be used to operate control-circuit switches.

There are applications of electric actuators on aircraft such as landing gear, wing flaps, engine-cooling control, bomb release, fuel valve, and so forth, where it may be necessary because of safety requirements to provide for emergency operation in case of gun-fire damage or other failure of the mechanical or electrical parts of the actuator system. In the case of landing gear it is common practice to provide either cable control that will release the gear from its actuator and allow it to extend by gravity or to provide for hand cranking of the actuator. In some applications such as wing flaps and engine-cooling-air control it will not be necessary to provide an actual mechanical override on the actuator but it will be sufficient only to install an emergency switch that will by-pass the normal control system and feed power directly to the motors. In the case of electrically controlled bomb racks and fuel valves, it is often required that a direct mechanical override be provided by means of cables.

In nearly all applications of electric actuators it will be necessary to provide against inadvertent operation. Control and power relays should be either of the balanced type or mounted in such a position that a severe acceleration in any direction likely to be encountered will not cause the relay to change the position of its contacts. It will also be necessary to avoid the use of relays that have their natural vibration period within the range of frequencies to be encountered in use. The use of sensitive relays in aircraft control should be avoided if possible, but in cases where they must be used the designer should provide shock-insulating mounts and water- and dust-tight covers.

In cases where the consequences of inadvertent operation of some piece of electric equipment would be very serious, the switches at the control station should be protected with plainly labeled and distinctively colored switch guards. In extreme cases it may be desirable to install two or more control switches in series.

In applications that require, by automatic control, the actuation of several different but related parts of the airplane in a certain definite sequence, it will be necessary for the designer to consider the possibility that the actuator may be stopped or reversed at some point other than the end of a cycle. The control must be designed so that under these conditions the proper sequence of operation is maintained.

There are locations in aircraft where explosive concentrations of gasoline vapor may be present under either normal or damaged conditions and it is very important that this point be considered when designing an actuator system. Motors and control equipment should be located where there is adequate ventilation and where these vapors cannot accumulate, but if this is not possible a weight penalty must be accepted because of the necessary explosion-proof construction.

Weight considerations are of paramount importance in the design of equipment of this nature and must be considered when the electrical and the mechanical schematic diagrams are being made. These schematics should be very complete and at this stage it is advisable to determine all gear ratios, screw leads, speeds of all elements, and all loads. There is an opportunity in a design of this nature to do a great deal of original thinking and the making of the schematic diagrams is the point at which most of the nonroutine work is done.

It should not be attempted in a paper of this length to give detail gear-design procedure but there are several basic considerations that may be covered. In the design of aircraft electric actuators the gearing most likely to be required will be straight spur, internal, bevel, helical, and worm. In work of this class the usual standard gear-design practice is not adequate because of the extreme conditions imposed by weight limitations, space requirements, high speed, extreme ambient temperatures, duty cycles, and life requirements. In these designs it will be necessary to call on experience and testing to a very large extent. Gear blanks may be forged from many of the SAE steels but, because of wartime shortages and restrictions, it may not always be possible to obtain those which are most suitable. In the more highly stressed high-speed applications probably the most satisfactory steel will be a high nickel such as 2515 because of its ductility, toughness, and resistance to fatigue. This is a carburizing type of steel and the gear teeth should be finish ground after heat treatment. Other very satisfactory gear materials will be found to be some of the chrome-molybdenum and the chrome-nickel-molybdenum steels which may be hardened without carburizing. Heat-treated blanks of these materials may be completely machined and then have the wearing surfaces hardened by the high-frequency induction process. As this method of surface hardening will result in almost no distortion it is only necessary that the gears be lapped to obtain extreme accuracy.

In the design of gears for this class of service considerable weight may be saved if forged blanks are used because the controlled grain flow and the small chance of having cracks or inclusions at critical sections will allow much higher stresses to be used.

In the design of these lightweight high-speed gears the designer must call on experience and testing to a very large extent and not follow too closely the accepted practices used in general machinery or automotive design. The best tooth form for the comparatively small highly stressed high-speed gears used in an airplane electric actuator differs from all of the accepted standards in that the pinion has a long addendum tooth which mates with a gear having a correspondingly short addendum.

This is an involute system using a 20-degree pressure angle with a full-depth tooth and has a number of advantages over the American Standard 20 degrees stub-tooth system. By having unequal addendums on the mating gears it is possible to have a pinion with a smaller number of teeth without the necessity of undercutting for mating tooth clearance. As a result the tooth will be thick at the root and will have a greatly increased beam strength. Another advantage of this system of gearing is that being full depth, the arc of action will be greater than that of the 20-degree stub-tooth involute system with the result that smoother action and lower tooth impact loads will be obtained at high speeds. This unequal addendum system of gearing is being considered at present for adoption by the American Gear Manufacturers' Association and has received much favorable comment.

In mounting gears the accuracy of alignment and the rigidity of shafting are of the utmost importance. It is necessary to place very close tolerances on the pitch cylinder tangency of straight spur and of helical gearing and on the pitch-cone tangency of bevel gearing because any inaccuracies in this respect will reduce the effective widths of the gears and reduce the power-transmitting capacity. The problem of obtaining accurate gear position is further complicated by the fact that the bearings on each end of a gear shaft are usually mounted in different pieces of the gear-housing assembly. Another factor that affects the accuracy of tooth engagement is the gear-shaft deflection due to the separating forces set up by the tooth pressure acting at

the 20-degrees pressure angle. It is necessary that this force be calculated for each engagement in the actuator gear train so that the lightest satisfactory shaft may be designed and also so that proper bearings may be selected and provided with suitable supports.

It is not considered practical in this class of work to provide for adjustment for straight spur or helical gearing because of the direction in which these adjustments would necessarily have to be made. When using straight or spiral bevel gearing it is usually advisable to back up the thrust bearings with an adjustment.

The practical limit on the face width for straight spur gearing will be found to be about four times the circular pitch and for bevel gearing about one third the slant height of the pitch cone.[1] The reason for this limitation in face width is the difficulty in obtaining accurate full-width contact at the tooth engagement. However, if it is desirable because of space or other limitations to increase the power-transmitting capacity these face width limitations may be exceeded by using very accurately finished gears and by providing very accurate and rigid mountings.

Planetary systems of gearing are often used in aircraft actuator design because of the high reduction ratios and power-transmitting capacities which may be obtained in a small space and also because the gearing is symmetrical about the center line of the system. In gearing of this type it is common practice to use two or more planet gears in order to get the maximum power transmitting capacity for the minimum weight of gearing and gear case. When multiple planets are used, the gearing and mountings must be very accurate in order to distribute equally the load between these planets. One point that must be borne in mind by the designer of planetary gearing systems is that when two or more planet gears are used the sum of the number of teeth in the ring gear and in the sun gear must be divisible by the number of planet gears in the system. If this rule is not observed the various elements of the system will not mesh properly with each other.

Very good efficiencies may be obtained using either simple or compound systems of planetary gearing, but the high reduction differential planetary is to be avoided if any considerable amount of power is to be transmitted. Because of the high circulating power within the differential system, the over-all efficiency is likely to be too low to be practical. This circulating power may be calculated by analyzing the system to obtain the pressures and velocities at each engagement and will be found to be many times the amount of power being transmitted through the system. As the power lost at each engagement is nearly one per cent of that entering the engagement, the efficiency of the system may easily be determined.

In calculating the strength of gear teeth for use in aircraft electric actuators, the conventional method using the Lewis formula is not found to be suitable for gearing of the quality described here because the weight would be excessive. This empirical formula is very conservative and will result in calculated stresses that are approximately eight times as great as those which are actually obtained in these high-precision gears.

Careful design and testing will show that most of the gears in an actuator of this type may be made with a rather thin web between the hub and rim and that this section may have lightening holes. Another weight-saving measure that is often overlooked is the use of hollow gear shafting.

It will be found in most cases that antifriction types of bearings should be selected for moderate to heavy thrust loads and for pure radial loads above 500 rpm. The higher-speed elements of gearing will most likely have relatively high values of diametral pitch requiring a high degree of mounting accuracy which can only be obtained through the use of ball, roller, or needle bearings. Another point in favor of the antifriction type of bearing is that less trouble due to lowered efficiency will be experienced at extremely low temperatures. The bearings for intermediate stages of straight spur gearing may be of the needle or roller type with the shaft ends held against thrust only by brass disks. When using needle bearings it is necessary that the loads encountered cause only extremely low values of shaft deflection in order to properly apply these loads to the needles and prevent their becoming jammed. When using an aluminum or magnesium gear case with highly loaded ball or roller bearings, it is good practice to press a steel bushing into the recess and use this bushing to support the outer race of the bearing.

It is important in actuators for this class of service to use lubricants that will operate satisfactorily in ambient temperatures from −65 to +160 degrees Fahrenheit. This problem of lubrication is at present receiving a great deal of attention by various research organizations and it is expected that an entirely satisfactory answer will be found in the near future. Seals must be used that will prevent lubricant from being lost from the unit and that will keep dust and water from entering while at the same time permitting breathing with changes in altitude.

In designing an electric actuator for aircraft the availability of all component parts must be considered. If the project is of sufficient importance and time will permit, it will be feasible to develop new materials and processes for use in manufacturing the unit. In alloting time for design, research, and testing, the designer must consider the production schedule that must be followed and the manufacturing facilities that can be made available.

The actual design of the actuator unit will very largely determine the cost of the units in production because of the effect on tooling, procurement of materials, and manufacturing facilities required. If the quantity of units to be produced is large it may be advisable to adopt designs that will involve large expenditures in special dies, molds, and other tooling which would result in a lower over-all cost.

In the total cost of a piece of equipment of this nature the designer should consider the cost in maintaining the unit in airworthy condition throughout its expected life which, of course, will include the loss in revenue incurred by having the airplane out of service for repairs.

As shown by this paper a very great effort must be made by the designer of an aircraft electric actuator to cut the weight to an absolute minimum consistent with the stated requirements. It is also shown that this is accomplished by ingenious design by the proper selection of materials, by the rigid control of processes, and by the insistence of a high standard of workmanship. Every ounce of weight that can be saved shows an increase in aircraft performance in terms of speed, range, and pay load.

Reference

1. Potential Breakdown of Small Gaps Under Simulated High-Altitude Conditions, M. J. DeLerno. AIEE Transactions, volume 63, 1944, March section, pages 109–12.

A 40-Kva 400-Cycle Aircraft Alternator

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Synopsis: A-c auxiliary-power systems for aircraft date back to World War I. Recently numerous technical articles have indicated an intense revival of interest in this subject. Heart of any electric system is the generator which supplies electric power to all parts of the system. An alternator, especially designed and built for 208-volt three-phase systems is described in this paper. Light weight and reliability dominate the design, which involved a large number of new and unusual problems.

ALTERNATING current has replaced direct current in large commercial power systems because large amounts of power can be transmitted long distances more economically; alternating voltages can be stepped up or down at will by a transformer; and the polyphase squirrel-cage induction motor, which employs no brushes, is so simple and reliable. These advantages, which also apply in aircraft use, have caused a long-continued interest in a-c systems.[1,6] While this interest is particularly strong now, it is to be remembered that the use of a-c systems in aircraft dates back to World War I when wind-driven alternators with built-in spark gap were used to supply power for radio transmitters. More than ten years ago 600-watt alternators driven by a main engine through a constant-speed drive, were used by the Navy. Prior to the present war two different a-c systems were tried experimentally in different planes: one of these was a single-phase 800-cycle system, the other, a three-phase 120-volt 400-cycle system. Rectified a-c systems with 30-volt d-c outputs of 200–800 amperes have also been proposed.[3]

Vernon Grant and Melville Peters showed in 1939 that the optimum voltage for the electric system of airplanes of 20,000 pounds gross weight or larger exceeds the present universal standard of 28 volts.[10] Because higher system voltages are desirable for large airplanes, alternating current appears preferable to direct current for high-altitude operation. Alternating current is easier to interrupt; moreover, the motors have no brushes and are generally lighter and more reliable. A-c power can supply 85 per cent of the total load and, in addition, it has many advantages as a power supply for aircraft radio.[9]

Weight, of course, is always a primary consideration in aircraft equipment. At a meeting of the AIEE Dayton Section in May 1944 an interesting tabulation of comparative weights of different electric systems was presented. The weight comparison was made for a hypothetical bomber of assumed dimensions, and an assumed gross weight of 150,000 pounds. This comparison is given in Table I.

Viewed in any light, the advantages of alternating current are so great that the development of the alternator described in this paper was initiated.

Alternator Rating and Requirements

Alternators of any desired rating can be built for aircraft service. This paper however, is limited specifically to the description of an alternator having a continuous rating of 40 kva, 208 volts, three-phase, 400 cycles, 75 per cent power factor. Overload ratings for this alternator are as follows:

60 kva at 100 per cent of rated voltage for five minutes.

80 kva at 90 per cent of rated voltage for five seconds.

From a heating standpoint, 150 per cent load for five minutes is a more severe requirement than full load continuously, but overload capacity, of course, is necessary to meet emergency conditions. The 80-kva rating was set up as a requirement for two reasons: to provide capacity for starting a number of motors simultaneously, and to ensure greater stability when carrying a load of 60 kva. These load ratings are based upon 250 cubic feet per minute of cooling air being supplied from a blast tube at not less than six inches water total pressure. An internal fan has been built into this alternator to provide cooling sufficient for a small amount of output—about 25 per cent of rated load—while operating on the ground without the benefit of blast cooling. The alternator is designed to supply a three-phase system with grounded neutral. Six leads are brought out to permit the use of differential protection, which disconnects the alternator from the system in the event of a short circuit, either at its terminals or inside the alternator.

Leads are tagged according to American Standards. *T*1, *T*2, *T*3 are line leads, whereas *T*4, *T*5, *T*6 are neutral leads. Alternator and exciter connections are shown in Figure 10. The terminal block is stepped to facilitate wiring connections and to avoid interference of leads. Without the stepped arrangement, the leads easily could be short-circuited at the block. A cover is provided for the terminal block to protect the leads and terminals.

Damper or amortisseur windings are incorporated to provide stability when synchronizing and when operating in parallel with other machines.

Careful consideration was given to selection of the operating speed, and 6,000 rpm finally was selected as the highest speed feasible for a machine of 40 kva. But the machine is designed to withstand an overspeed requirement of successful mechanical operation at 9,000 rpm, to correspond with the maximum speed of the drive shaft.

Alternator Size and Mounting Arrangement

All important outline dimensions of this 40-kva alternator are shown in Figure 3. It will be noted that the length is 19 inches and the diameter of the body is nine inches. The 19-inch dimension includes the air connector shown on the right, but does not include the shaft extension and pilot fit on the left. These



Figure 1. Front view of 40-kva alternator showing air connector and mounting band

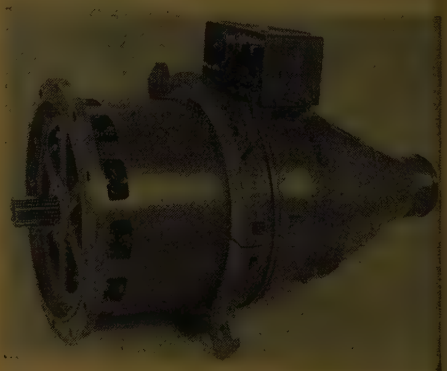


Figure 2. Rear view of 40-kva aircraft alternator showing bracket and shaft extension

Paper 44-190, recommended by the AIEE committee on air transportation for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted June 22, 1944; made available for printing July 11, 1944.

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The authors acknowledge the assistance of J. C. Cunningham, L. A. Kilgore, and E. C. Whitney of the East Pittsburgh Works, and J. D. Miner of the Lima Works, of Westinghouse Electric and Manufacturing Company.

outline dimensions as a whole conform to specifications of the United States Army Air Forces. The alternator is designed to bolt to the constant-speed drive to make, in effect, a single unit. Pilot fit and splined shaft of the alternator engage corresponding parts of the drive.

Figure 3 also shows an adjustable two-piece mounting ring. This ring with its four projecting ears serves to support the alternator end of the combined unit in the airplane framework. Adjustability of the ring over a range of four inches axially facilitates installation. The body of the alternator, over which the ring is adjustable, is machined to a smooth, accurate, cylindrical contour, so that the ring may be secured firmly in any position within the limits of adjustment.

Coupling to the constant-speed drive is through a replaceable 24-tooth driving spline. Some radial freedom, to allow for a small amount of misalignment between the alternator and constant-speed unit, is provided by use of a stub shaft, the opposite end of which is spline-fitted to the inside of the main shaft of the alternator. A shear section is provided in the stub shaft for protection of the constant-speed drive in case of combat damage or mechanical failure of the alternator. Removal and replacement of the stub shaft does not require dismantling the alternator. If broken, the stub shaft can be driven out easily by inserting a rod in the hollow shaft at the air-connector end of the machine.

Method of Excitation

Excitation is furnished by an integral d-c exciter of conventional aircraft-generator construction, an arrangement chosen after a careful study of all possible methods of excitation. This arrangement is lightest in weight, is less complicated, requires the smallest regulator, and is more stable than any other method considered. On the *XB*-19 airplane, excitation power was obtained from the a-c output, which was rectified and controlled electronically.[2] The latter arrangement

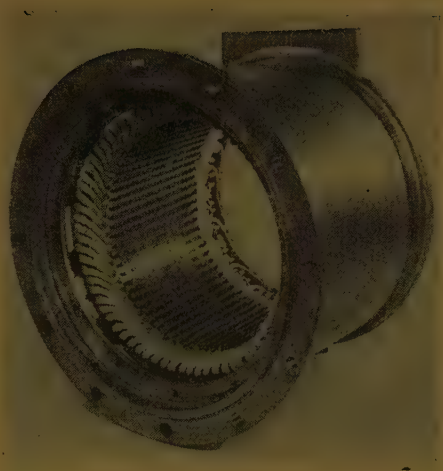


Figure 4. Wound stator before impregnation

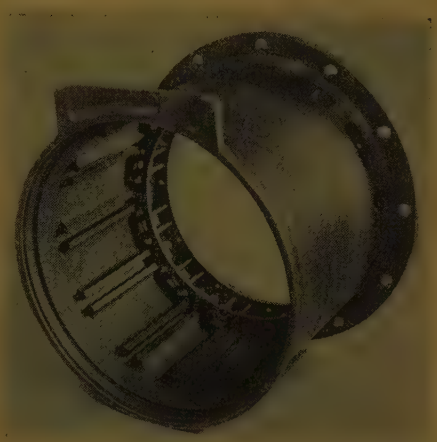


Figure 5. Magnesium-alloy frame of alternator showing internal ribs

inherently means loss of excitation in the event of a fault, and the alternator will not provide enough current to burn clear many types of faults which might occur. This arrangement had the further disadvantages of added weight, and also of appreciable warm-up time for the electronic tubes. Use of an inductor alternator to supply excitation power was considered because of the advantage of eliminating brushes. However, an inductor alternator is heavier than a d-c machine and requires the use of a rectifier, which would need to be mounted in or on the alternator in order to make the latter interchangeable with another unit which might use a d-c exciter. Such an arrangement would be awkward and heavy.



Figure 6. A-c stator coils

Excitation might have been furnished from batteries or from the d-c power

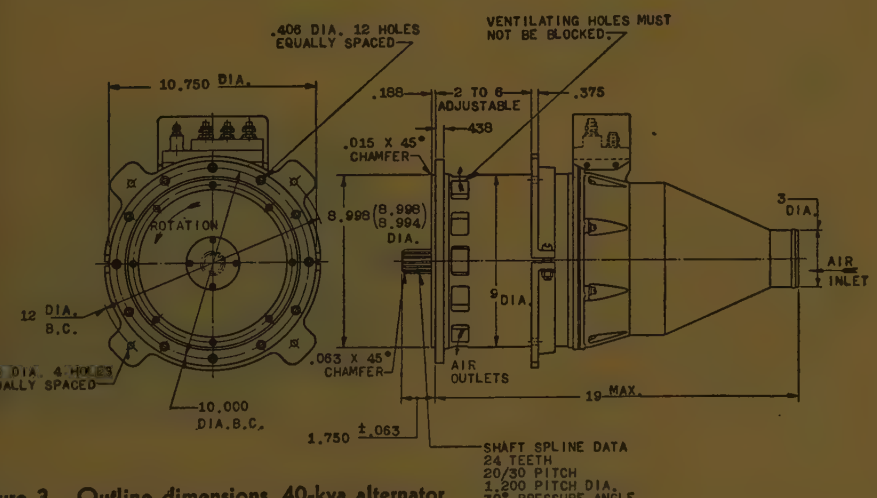


Figure 3. Outline dimensions, 40-kva alternator

Table 1. Comparative Weights of Electric Systems

Components	System: 28-Volt D-C Self-Excited Generator	120-Volt D-C Self-Excited Generator	208/120-Volt 400-Cycle 3-Phase Built-In Exciter
1. Generator weight, one unit	150 lb.	120 lb.	80 lb
2. Voltage regulator, one unit	6 lb.	6 lb.	9 lb
3. Main generator breaker, or switch	6 lb.	6 lb.	5 lb
4. Drive shaft from engine	15 lb.	15 lb.	15 lb
5. Constant - speed drive			90 lb
6. Total, items 1 to 5	177 lb.	147 lb.	199 lb
7. Total weight of four units	708 lb.	588 lb.	796 lb
8. Batteries, total weight	55 lb.	55 lb.	55 lb
9. Two 28-volt 6-kw d-c sources of power	0 lb.	127 lb.	100 lb
10. Two 12-kva sources of single-phase 400-cycle power	300 lb.	280 lb.	0 lb
11. Four sets of automatic synchronizing control			40 lb
12. Weight of main power wiring	420 lb.	55 lb.	65 lb
13. Total weight excluding utilization equipment	1483 lb.	1103 lb.	1056 lb

Airplane is a long-range bomber, weighing 150,000 pounds; continuous electric load of 60 kw. Wing spread, 170 feet; length of fuselage, 110 feet; 45 feet to outboard engine; 28 feet to inboard engine. Generator specification:

30 kw continuous, 45 kw for five minutes
Speed range, 3,000–9,000 rpm
Not engine-mounted
Blast-cooled
Altitude, 0 to 50,000 feet

This table was developed at a round-table conference conducted by Major W. A. Barden, United States Army Air Forces, equipment laboratory, Wright Field. Members of conference were: George W. Sherman, Wright Field; L. G. Levoy, R. H. Kaufmann, General Electric Company; J. C. Cunningham, D. E. Fritz, C. G. Veinott, Westinghouse Electric and Manufacturing Company.

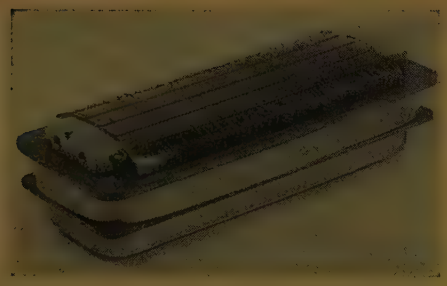


Figure 7. Complete pole and field coil of alternator

system of the airplane. Such an arrangement has three principle disadvantages:

1. A fault in the d-c system would cause failure of the a-c system.

2. A larger and heavier voltage regulator would be required, involving a major development, as such a regulator is not now available.

3. There would be an excessive waste of power in the regulator.

Brush life at high altitude—the principal objection to the use of the d-c exciter—was no longer considered the problem that it once might have been, since a successful altitude treatment for brushes is now available.[4] Based on flight experience with d-c generators and altitude-chamber tests on the alternator, the expected brush life at 35,000 feet is approximately 500 hours.

Electrical Design Considerations

Fundamental principles employed in the design of aircraft alternators and d-c generators are the same as those used in the design of standard industrial machines. The elements of an aircraft electric machine resemble those of an industrial machine of far greater physical size. However, in the design of aircraft electric machines, extreme effort is put forth to obtain a given rating at the lowest possible weight; thus, many refinements are required in the electrical and mechanical design which are unwarranted in standard machines.

Electric conductors of aircraft alternators, as well as those of engine-mounted d-c generators, are operated at current densities far in excess of standard machines; 10,000 amperes per square inch is not uncommon[4]—a figure four times as high as used in continuous-rated industrial machines. Magnetic circuits of this 400-cycle aircraft alternator are worked at about the same densities as conventional 60-cycle machines. Because of the much higher frequency, however, the watts loss per pound of magnetic material is from 15 to 20 times as high as in a standard 60-cycle machine. Although the copper and iron are worked harder in aircraft than in industrial machines, the efficiencies of the former are as high or higher for comparable ratings. This is possible because of greater refinements in design, higher operating speeds, and better ventilation of aircraft machines.

Insulation of the windings of aircraft alternators must be suitable for the temperatures encountered. Because indefinite life is not expected and could not be obtained without a great increase in weight, class *A* and class *B* insulating materials usually are operated at higher temperatures in aircraft than for industrial service. Thus, for the life expected of an aircraft alternator, class-*A* insulation can be operated at 130 degrees centigrade and class-*B* insulation can be operated at 150 degrees centigrade.[4] Each type of insulation has been used in this alternator only where its maximum permissible temperature will not be exceeded.

D-c generators for aircraft use are rated at 28.5 to 30 volts, whereas the 208-volt alternator described in this paper is operated at 120 volts to ground. Because of the higher dielectric stresses, the a-c winding is given a potential test to ground of 1,500 volts (rms), and a potential test between phases of 500 volts; alternator field and exciter armature are tested at 500 volts to ground.

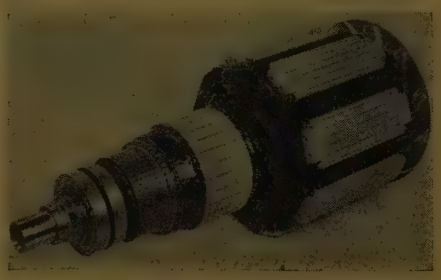


Figure 8. Complete rotor showing alternator field, slip rings, exciter armature, and commutator

As in the d-c system, greatest reliability is obtained with parallel operation,[2] and the alternators are designed accordingly. As mentioned before, a damper winding is placed in the pole faces to provide more stable operation of the alternators under any operating condition which might cause hunting. This winding, made of copper, is similar to that used in a large synchronous machine.

Stator Construction

Figure 4 shows the alternator stator with laminations and winding in place, and Figure 5 illustrates the lightweight frame. Internal ribs support the laminations and allow axial passage of ventilating air over the stator core.

Circular laminations without studs or rivets are used, permitting skewing which improves the wave form. To ensure exact alignment, laminations are stacked on an accurately ground mandrel, which has guide bars for aligning the punchings and obtaining the correct skew. While still on the mandrel, the punchings are compressed axially, and a slot is machined in the outer periphery parallel to the axis of the stator. A corresponding slot is machined in one of the ribs of the magnesium-alloy frame, and a rectangular key fits into both slots, thereby locking the punchings.

Stator laminations are held in compression between a shoulder in the frame and a spacer ring held in place by means of six axial through bolts. Openings are provided in the ring and shoulder so that ventilating air can enter the axial ducts between laminations and frame.

A-c stator coils are formed of double-glass-covered rectangular copper wire. Two of these coils are shown in Figure 6. Accurate forming assures that the coils will not touch each other at the ends and that they will have the strength necessary to withstand short-circuit stresses. Class-*B* insulation is utilized in the a-c winding. Paper-and-mica slot cells are used, the paper backing merely providing strength during assembly. Glass sleeving is slipped over the exposed portion of each stator coil. Coils are anchored in slots by means of trapezoidal wedges. After winding, the a-c stator is impregnated with an alkyd resin varnish to resist the moisture, dirt, and fungus growths which an aircraft alternator may encounter.

Rotor Construction

Two obvious methods for constructing the rotating field of alternators such as described in this paper are one-piece laminations and separable poles. With one-piece laminations, the poles and pole faces are integral with the yoke; this type of construction has been used with success in many small machines and in a few large four-pole industrial machines. Since the yoke and poles are continuous, a good magnetic circuit is obtained through the rotating field. However, the one-piece rotor punching has the disadvantage of requiring a random field winding, which is satisfactory in small low-speed machines, but seldom if ever, is used in large high-speed machines. This con-



Figure 9. Alternator with air connector removed, showing front bracket and rocker ring

struction was not adopted in the machine described in this paper because of low mechanical strength and poor space factor inherent in a random winding.

Figure 7 illustrates the separable-pole construction used in this machine. Pole laminations have a rectangular hole in the body, by means of which they are assembled onto a snug-fitting rectangular bar of iron having good magnetic properties. These laminations are clamped together by means of the damper bars, a steel through rivet, and aluminum-alloy pieces which also serve to support the coil ends. Holes are now drilled and tapped through the punchings into the rectangular bar previously described. This construction permits the field coils to be wound directly on the poles.

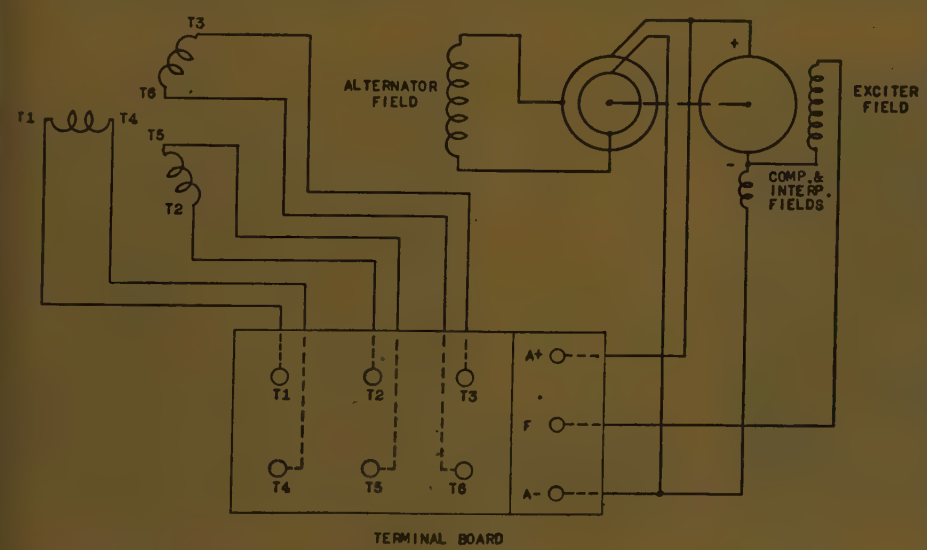
A heavy layer of fish paper, glass, and mica insulates each coil from the body of the pole. Each coil is wound in two sections of thin copper strap insulated between turns with glass tape. Micarta washers insulate the upper section of the coil from the pole and the coil sections from each other.

After the coils have been wound, they are bolted to a cylindrical yoke and then connected between poles at this time in order to avoid disturbing them later. Wedges are now inserted between adjacent coils in both upper and lower sections. After connecting the coils and inserting wedges, the rotor is thoroughly vacuum-impregnated with an alkyd resin varnish and then baked. Thus, a solid rotor winding is obtained, free of air pockets and impervious to moisture.

Alternator field, exciter armature, commutator, and slip rings form an integral unit. The exciter armature is pressed onto the long shaft, flanged at one end and bolted to the yoke of the alternator field. A shorter flanged shaft is bolted in a similar position to the opposite end of the yoke. Thus, the complete rotor utilizes only two bearings. This construction greatly simplifies assembly of the alternator and prevents any possible internal misalignment.

Figure 10. Internal wiring diagram and terminal arrangement of 40-kva alternator, including exciter

When rotation is clockwise (at commutator end), phase sequence is T1, T2, T3



Exciter Construction

The front bracket of the alternator supports the stator of the exciter, as well as the rotor, bearing, and brush assemblies. This construction is illustrated in Figure 9, a view of the alternator with the air connector removed, showing the arrangement of the exciter in the bracket. Exciter brushes and slip-ring brushes are all carried on the rocker ring, which is mounted in the front bracket. By reference to the schematic wiring diagram, Figure 10, it can be seen that the slip-ring brushes are connected directly to the output of the exciter, thus requiring minimum wiring between the exciter and alternator field. To provide maximum excitation for the alternator when required, a compensated exciter with interpoles is used. Two collector rings are mounted at the front end of the rotor, adjacent to the exciter commutator. The rings are shrunk over an insulated steel bushing, which later is pressed onto the exciter shaft.



Figure 11. Terminal board and studs, showing stepped arrangement

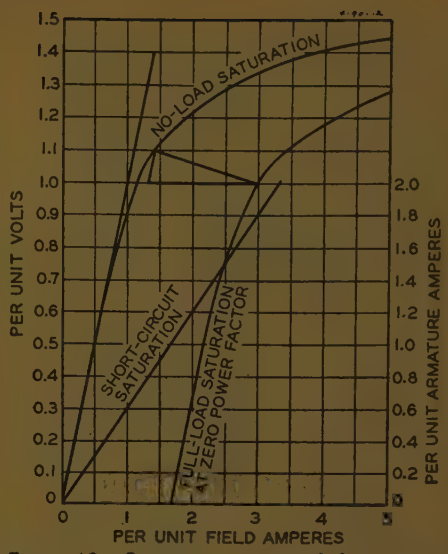


Figure 12. Saturation curves and short-circuit characteristic of 40-kva alternator at 6,000 rpm

Mechanical Design Considerations

Aircraft generators must operate with minimum attention and trouble. No part of a generator is more important to reliable operation than the bearings, and careful consideration must be given to their selection. Sealed or shielded anti-friction bearings offer excellent possibilities for aircraft generators, because they require no attention. Bearings are lubricated at the factory with grease of proper grade and quality to assure long life. When the lubrication is properly applied, a bearing life of several hundred hours may be expected. Because it is in the path of the incoming air, the front bearing is unlikely to become overheated. However, special precautions have been taken to make certain that the rear bearing is adequately cooled. The exciter shaft is hollow, and the front end is open to the incoming air, while at the rear end four holes are provided in the shaft flange. Cooling air passes through the rotor, and then over the rear bearing, thereby preventing excessive temperature rise. This ventilating path serves a second purpose, that of helping to cool the rotor yoke.

To obtain minimum weight, both the front and rear brackets are cast of magnesium alloy. The rear bracket has six arms, channel-shaped for increased strength. The cone-shaped front bracket has four ribs which serve to support the the exciter stator and rocker ring, as well as the front bearing.

Engine-mounted d-c generators are subjected to more severe linear and torsional vibrations than encountered in any other type of service. History of the engine-mounted d-c aircraft generator records a great amount of progress in the solution of the problems involved. Be-

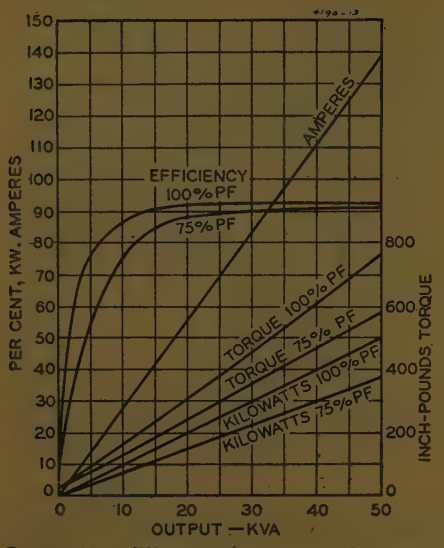


Figure 13. Efficiency, torque, and output curves of 40-kva alternator at 6,000 rpm, from dynamometer tests

cause the alternator of this paper is not engine-mounted, the problem of linear vibrations and accelerations is less severe; however, the machine is subjected to all the vibrations and accelerations occurring in the nacelle structure. Torsional vibration presents a severe problem of undetermined magnitude, because torsional impulses may be transmitted through as well as developed in the constant-speed drive. If the generator is mounted on an auxiliary engine, severe vibrations, both linear and torsional, may be encountered.

Ventilation and Cooling

Ventilation is unquestionably one of the principal factors influencing the weight of any aircraft generator. Without blast-tube cooling,[5] the extremely high outputs per pound of modern aircraft generators would not have been possible. Aircraft-generator builders have been hampered by lack of accurate information on the pressure–volume characteristics of practicable aircraft blast-tube systems. Shall the generator designer use restricted ducts in his machine, or shall he make them as wide open as possible? Few problems are more difficult to solve than the precise answering of this question. In general, the generator designer knows that, with blast-tube cooling, the less air his machine uses, the more total pressure head is available at the generator; and, conversely, that a wide-open generator which uses more air results in less available total head across the generator. Total available head and volume of air could be determined by the generator designer, as pointed out in a recent AIEE paper,[5] from a pressure–volume curve of the blast-tube installation, if the latter were available.

Some general considerations on the qualitative differences in the cooling problem of wide-open and restricted generators will assist in an understanding of the ventilation problem. The significant factor is total temperature rise of the winding hot spot above ambient, which is the sum of two temperature rises:

1. Rise of cooling air passing through the machine.
2. Rise of hot spot above the cooling air.

A wide-open generator passes more air; hence, rise of cooling air is invariably less. But, in the wide-open generator, the dissipating area of the cooling ducts is less, and air velocities in the ducts tend to be lower; the net effect is to cause a higher rise of hot spot above the cooling air. Since total temperature rise is the important factor, it can be seen that the minimum sum of the two factors occurs when the generator is neither too wide open nor too restricted. A complete answer to the ventilation problem requires an analytical or empirical evaluation of the hot-spot rise for different degrees of restriction, taking into account the pressure–volume characteristic of the blast tube.

For the 40-kva alternator discussed in this paper, a total head of at least six inches must be maintained between the inlet and outlet. If the alternator is installed in a large duct which supplies air to the main engine, an exhaust shroud around the outlet ducts of the alternator has to be provided, because the entire alternator is surrounded by air at a relatively high pressure; this shroud must discharge to a region of lower ambient air pressure.

Air ducts have been provided between the outside diameter of the stator punchings and the frame. Air which passes through these ducts also passes directly over the end windings, picking up heat from them as well as from the stator laminations, and from the frame which is a good conductor of heat. Cooling air also flows axially between the field coils of the rotor. The front bearing is cooled by the main blast of air as it enters the alternator, whereas the rear bearing is cooled by air which flows through the hollow shaft, as explained previously.

No effort has been spared to provide the best cooling obtainable with the specified total head of six inches, and the temperature rise has been held down to permissible limits. But it must not be overlooked that, however effective the ventilating system may be, for any given pressure, better results will be obtained from the alternator if higher pressure or more cooling air is made available. By better results are meant lower temperatures of the alternator, with consequent longer life of insulation and bearings, and ability to carry higher overloads of short-time duration.

Performance

Saturation curves of the alternator are given in Figure 12. These curves include: a no-load saturation, a zero-power-factor saturation, and a three-phase short-circuit saturation. Dynamometer tests are recorded in Figure 13, which shows efficiency, torque, kilowatts output, and amperes output, plotted against kilovolt-ampere output. Efficiencies reported here include losses in the exciter, voltage regulator, and alternator itself; they were computed from direct measurements of input and output.

Conclusions

A number of experimental 40-kva 400-cycle aircraft alternators has now been built, and extensive tests, in addition to those reported in the paper, have demonstrated that such machines are ready for use in aircraft as soon as a constant-speed drive or other suitable driving means is made available. (Some of the tests not reported in the paper include: short-circuit tests, three-phase, line-to-line, and line-to-ground; altitude-chamber tests of heating and brush wear; parallel-operation tests; overspeed tests; starting a heavy load, such as a motor generator set.) A weight of 85 pounds for this 40-kva alternator, compared with a weight of 47 pounds for a nine-kilowatt d-c air-



Figure 14. Comparison of 40-kva alternator and 300-ampere type *R*-1 d-c generator

Aircraft Electrical Horizons

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AN ENGINEER needs a goal. In this respect he is no different from the follower of any other profession, art, or trade. Having a goal, every effort and project becomes a step in the final achievement. It is the purpose of this paper to suggest some goals which are on the horizon today. When these goals are reached, it is quite likely that new horizons can be seen. However, let tomorrow take care of itself.

A goal is not only an incentive, it is a pathway. Seeing the need for a device is more than half the act of inventing that device. It can well be that presentation of some of aviation's needs will stir the inventive genius of some engineer who will then create a solution. It is by such steps that we will extend the horizon.

The importance of the electric system looms large today. In former installations the hydraulic and electric systems were not essential to flight. Soon this will no longer be true. Already designers of automatic control apparatus are placing faith in the aircraft electric system. If an inverter delivering a-c power to a turbo supercharger regulator should fail, engine manifold pressure would no longer be controlled; it happens that the engine air-induction system is unstable and a dangerous operating condition can be created in a few seconds. The source of a-c power must not fail. This is typical of situations that emphasize reliability of the electric system.

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Electric-System Protection

Hand in hand with proper reliability of separate electrical components is the necessity for automatic protection of the system from the power sources through feeders and circuits to the individual items of equipment. The state of the latter art is not at all satisfactory; almost without exception protective devices have one or more of the following faults:

1. They do not duplicate the thermal ability of the apparatus being protected.
2. They are too heavy.
3. They have serious errors due to ambient temperature.

For example, circuit breakers are used to protect the cable (not the equipment) of a circuit. A given rating of circuit breaker should permit the cable to carry every ampere that it can safely carry. In fact, if the perfect circuit breaker were developed, it would be designated by an American wire gauge number.

Generators and Motors

The electrical industry can be proud of the progress that has been made in aviation accessories during the last few years. The output from a cubic inch of space and material has been doubled, quadrupled, and doubled again. The engine-driven generator is the best example of this progress. Almost since the beginning of modern aviation the generator designer has been confined to a space of 6 inches diameter and 14 inches length, a volume of 400 cubic inches. Until 1939 this space produced 750 watts, less than 2 watts per cubic inch. Today, nearly 30 watts per cubic inch are developed in that space. The output constant for this type of machine is approaching 20,000, approximately three times the conventional value for the equivalent rating. This constant is derived from the relation

$$c = \frac{D^2LN}{kw}$$

where D and L are dimensions of the active material in the armature, N is speed in revolutions per minute, and kw is output in kilowatts.

At the same time good efficiency has been maintained. At first glance this is surprising since the power loss per pound of material is much higher than in industrial practice. But the number of pounds is reduced until efficiencies are tolerable. This factor is important to aviation; low efficiency means a higher power consumption and this in turn requires the use of fuel at a greater rate. Gasoline weighs six pounds per gallon and an engine requires nearly 0.5 pound per horsepower hour. A generator having an efficiency of 70 per cent will require one pound of fuel per kilowatt-hour.

However, industry has been too prone to modify an existing article to meet a given aviation need. This may be justified in cases of extreme haste or where cost of development must be avoided. But it must be remembered at all times that in aviation a pound saved may be worth its weight in gold.

Similar progress has been made in d-c motors. Just as the specific weight of generators has been reduced to four pounds per kilowatt, so has the weight of motors been reduced to four pounds per horsepower at comparable speeds. Still lower weights are on the horizon and one of the steps to obtain them is higher speed. This will require further development in bearings, lubrication, and commutator design.

craft generator, indicates that electrical outputs of generators still can be expected to increase faster than the weights. Experience gained in the building and testing of this alternator can be used to develop better and lighter machines, or machines of still higher rating; the limit of ratings of aircraft generators is not even in sight!

Perhaps, in the future, direct-connected alternators can be used to maintain aircraft engines in synchronism. For test purposes, these 40-kva alternators were direct-connected to 450-horsepower aircraft engines; after the alternators were synchronized manually, it was found that they would hold the 450-horsepower engines in exact synchronism over a wide range of throttle settings, even while carrying electric loads at the same time.

References

1. WHY TOMORROW'S PLANES WILL HAVE A-C SYSTEMS, T. B. Holliday. *Electrical Manufacturing*, volume 32, July 1943, page 88.

2. PARALLEL OPERATION OF AIRPLANE ALTERNATORS, D. W. Exner. AIEE TRANSACTIONS, volume 62, 1943, December section, pages 755–60.

3. AIRCRAFT ELECTRIC POWER-SUPPLY SYSTEM, J. E. Yarmack. AIEE TRANSACTIONS, volume 62, 1943, October section, pages 655–8.

4. TESTING AIRCRAFT GENERATORS, H. E. Keneipp. AIEE TRANSACTIONS, volume 63, 1944, March section, pages 105–09.

5. BLAST-TUBE COOLING FOR AIRCRAFT GENERATORS, C. G. Veinott. AIEE TRANSACTIONS, volume 63, 1944, July section, pages 520–5.

6. BASIC CONSIDERATIONS IN SELECTION OF ELECTRIC SYSTEMS FOR LARGE AIRCRAFT, W. K. Boice, L. G. Levoy, Jr. AIEE TRANSACTIONS, volume 63, 1944, June section, pages 279–87.

7. A-C AND D-C SHORT-CIRCUIT TESTS ON AIRCRAFT CABLE, J. C. Cunningham, W. M. Davidson. AIEE TRANSACTIONS, volume 63, 1944.

8. EFFECT OF ALTITUDE ON ELECTRIC BREAKDOWN AND FLASHOVER OF AIRCRAFT INSULATION, L. J. Berberich, A. M. Stiles, G. L. Moses, C. G. Veinott. AIEE TRANSACTIONS, volume 63, 1944, pages 345–54.

9. ALTERNATING VERSUS DIRECT CURRENT FOR AIRCRAFT-RADIO POWER SUPPLY, D. E. Fritz, C. K. Hooper. AIEE TRANSACTIONS, volume 63, 1944.

10. OPTIMUM VOLTAGES FOR AIRLPANES, Vernon H. Grant, Melville F. Peters. ELECTRICAL ENGINEERING, volume 58, October 1939, pages 428–31.

11. DESIGN CONSIDERATIONS FOR D-C AIRCRAFT GENERATORS, J. D. Miner, Jr. AIEE TRANSACTIONS, volume 63, 1944.

Communication Equipment

Much that has been accomplished in the field of radio cannot be unveiled at this time. The importance of these developments can be realized, however, when it is noted that there are as many as 12 complete radio equipments in a large bomber. Each of these is designed for a special task and only a few of them are for radio communication as we know it. These new devices extend the horizons again for each of them presents an interesting peacetime application. And again, the aviation engineer is hoping that the radio engineer has a weight horizon. Much of this new apparatus is unnecessarily large and heavy. This handicap has been overlooked in the necessary haste to obtain useful devices for the war effort. But thoughtful study will eliminate many pounds of equipment weight and much aerodynamic drag from external antennas. Antennas appear to weigh only ounces, but drag increases the horsepower required from the engine and this in turn causes the consumption of many extra pounds of fuel.

Aircraft Lighting

The lighting of aircraft has never been perfect. While much has been done, far more remains undone. The position lights are adequate only in fair weather. A good landing light for fast fighter types is needed. Really satisfactory lighting for the instrument board and the pilot's compartment are yet to be produced. It is unlikely that any passenger is happy with the reading lamps in the modern transport. Failure of a landing-light lamp during testing prior to take-off is cause for a delay since the pilot is compelled to return to the hangar for a lamp replacement. A reserve is needed.

The problem of lighting instruments and the pilot's compartment is the most severe. Many minutes are required for the human eye to accommodate itself to darkness. Yet this dark adaptation can be lost in a second if a bright light is turned on in the cockpit either by design or accident. The lighting of instruments and important controls must be such that they are easily visible without impairing night vision.

Inverter Power Supplies

The development of inverters, direct current to alternating current, has made it possible to utilize alternating current for many small devices, particularly controls and instruments. Both of these uses are worthy of comment.

The inverter has usually been made as a motor generator and the resultant weight and low efficiency are a severe penalty. The penalty is the more noticeable when it is remembered that electric energy is put into the machine and electric energy is taken out. Perhaps, over the horizon is a way of avoiding the change to and from mechanical energy. The vibrator type of inverter has been successfully used at very low power and at frequencies ranging from 60 to 400 cycles per second. At the higher frequency this type will deliver very little power, although this frequency is most suitable for aircraft.

A-c power is well suited to many positioning and control devices since the controlling voltage can be amplified readily. The principle of the self-synchronous motor has been used to advantage in instruments which deliver a remote indication.

Relays and Switching Devices

Other items which receive too little attention are switching devices, and yet all the reliability of the system depends on them. Electric apparatus in aircraft all too often is the cause of an unsatisfactory report, because a relay failed to close, switch contacts welded, moving parts corroded, or any one of several other faults. Practically all of these can be traced to dirt, moisture, or salt air. And all of these can be avoided by hermetically sealing the switch, relay, or magnetic contactor. At the same time this change in design will remove all restrictions on altitude ratings, will make the device explosion proof, may permit an increase in rating, will increase reliability, and it is believed that this can be done at no increase in weight. In fact, weight should decrease if the rating can increase. This type of construction may be a basic requirement for switching devices in the near future.

Load and Equipment Considerations

As many as 150 electric devices have been used in several types of aircraft. This indicates the reason why the electrical load and the installed generator capacity per airplane have increased and are still increasing at a phenomenal rate. As an approximate rule it can be said that the generating capacity in kilowatts should equal the gross weight of the airplane in tons. And in terms of engine power, two per cent of the rated power or three per cent of the cruising power should be reserved for the generator.

In producing these devices the electrical engineer has gone far toward limiting conditions in his use of materials. For example, only the best of magnetic materials are used and these are worked to the limit; the best of materials are fully justified because they save weight. Weight saved is invaluable and the cost of materials is small. An airplane in the transport or bomber class costs $5.00 to $7.00 per pound. Manufacturing costs should be judged by that standard. The aviation engineer hopes that a good magnetic material having the specific gravity of magnesium is just over the horizon.

The current density in conductors has been pushed to new values. Ten thousand amperes per square inch and 125 circular mils per ampere are often used in continuous-duty apparatus. This requires careful engineering to provide adequate cooling, but the net gain is a considerable reduction in weight. For intermittent-duty applications theoretical current densities of 40,000 amperes per square inch have been used. Better conductors will permit higher densities, or still better cooling will do the same. At one time the use of silver instead of copper was proposed, but this was done not as a matter of conductivity but one of supply.

The current density in carbon brushes is limited only by temperature. Where densities of 40–60 amperes per square inch are conventional, values of 200 amperes per square inch at rated output are common in aircraft generators. With adequate cooling, densities of 600 amperes per square inch are on the horizon.

Power-Distribution Systems

As airplanes increase in size and electrical load, a decision about higher voltages is forced on the aviation industry. The standard for military aircraft is the 24–30-volt d-c system. This standard is likely to include the postwar transport and to supersede the present 12–15-volt d-c system. However, the 24–30-volt system is inadequate when 25–100 kilowatts are being considered and when airplanes with wing spans of 150 feet and more are discussed. A conductor to carry 100 amperes a distance of 100 feet with a voltage loss of 0.9 volt (three per cent of 30) is altogether too heavy.

Since it is a miniature model of a public utility, the electric system of a multiengine airplane presents many similar problems. An increase in system voltage has the same advantages to the aircraft as to the utility company. It is advantageous because it reduces the weight of wiring faster than voltage increases. Theoretically, the wiring weight should be inversely proportional to the square of the operating voltage. This reduction cannot be realized because the insulation becomes a greater proportion of the total conductor weight as size is decreased and because it is not practical to use a wire size of less than number 20 American wire gauge. The latter limitation is due to the structural strength of the conductor.

Two higher-voltage systems are already on this side of the horizon. One is direct current and one is alternating current. The d-c system has much to recommend it; it is similar to the 24–30-volt system already in use. Problems of commutation and switching which were forecast as trouble makers have not been difficult on actual encounter. The progress already made in reducing weight of 24–30-volt equipment applies

equally well to similar items of higher voltage. One of the largest users of electric power is radio, and by a judicious choice of voltages and care in the design of the radio equipment the need for inverters, dynamotors, and power packs can be avoided.

A big advantage of the d-c system is that it is possible to use a battery as a reservoir of energy and as a means of absorbing surges. The ampere-hour capacity of the cells should equal about half the current rating of the generator and there should be as many batteries as there are generators if the battery is to fill its place as a power reservoir properly. The proposed battery would consist of two 48-cell units if the lead-acid type were used. Unfortunately, battery weight for constant watt-hour capacity increases with increasing rated voltage.

The choice of system voltage is a most important problem. At least a portion of the power should be available at the familiar 110–120-volt level to use or adapt apparatus already developed for industrial use. At the same time higher-power loads such as large motors can advantageously use a still higher voltage. This suggests the familiar three-wire system at 230/115 volts. In metal aircraft the neutral wire can be omitted by using a structural ground.

A-C, D-C, and Hydraulic Comparisons

The a-c system has had little practical experience to support claims advanced for it in aviation engineering, although the potential advantages are similar to those which caused its adoption for industrial power. It has several disadvantages to overcome. Paralleling of generators is essential and power usually must be taken from the main engines. Since the latter will seldom operate in synchronism, a speed- and torque-conversion drive is necessary. The drive and generator will undoubtedly weigh more than a d-c generator of equal rating.

Power factor increases the current and therefore the conductor weight. Using a three-phase system, the operating voltage must be at least 40 per cent higher than that of the equivalent d-c system to result in the same weight of conductor. The equivalent of the d-c system described above would be the 208–120-volt four-wire three-phase system and the weight of conductors for equal power would be higher.

Offsetting these disadvantages are the lower weights of a-c apparatus, simpler construction, reduced arcing hazard, improved efficiency, reduced maintenance, flexibility of voltage, and possibly greater reliability and longer life.

Electric apparatus has many advantages over hydraulic and pneumatic equivalents that almost force use of the former for many of the aircraft accessory tasks. Some of these advantages are:

1. Higher efficiency.
2. Lower cost.
3. Ease of installation.
4. Simpler maintenance.

All of these are debatable with a proponent of any other method, but they are the advantages which the electrical engineer must exploit.

On the debit side, the electrical engineer has not delivered enough

1. Reliability.
2. Life.
3. Reduction in weight.

To provide adequate reliability the device must never fail in service and should require no attention for 1,000 hours of airplane operation as a minimum. With simple overhaul, the useful life should be at least five of these 1,000-hour periods. As for weight, a part is always too heavy. In addition, the electrical engineer must fit his device into the needs and requirements of the aviation industry and the operations of both airline and military aircraft.

In 1930 the operating cost for airline transports was approximately 60 cents per ton mile. In 1940 it was 30 cents. Perhaps in 1950 it will be 15 cents. Anything which makes that cost a reality will be welcome. By 1950 it may be possible to operate engines at a fuel consumption rate of 0.37 pound per horsepower-hour instead of the present rate of 0.45–0.46. This economy of operation will require automatic controls, and electric devices are ideal for such applications.

An airplane earns dividends only when it flies. The airlines will want a utilization factor of 60 per cent. This means that the airplane will be in service six hours out of ten. This leaves no time for needless maintenance of awkward electrical accessories.

Other Tasks for Electric Devices

The foregoing remarks have presented attainable goals and requirements. Some other big problems are also on the horizon. For example, one accessory which the electrical industry has never produced is the aircraft wheel brake. For the most part this task is performed hydraulically; it has also been done with pneumatic power. To present the requirements of this problem the assistance of the Goodyear Tire and Rubber Company was sought; R. J. Lammertse and C. E. Ricker, engineers of the wheel and brake division, kindly furnished information on which the following is based. Emphasis has been placed on the particular phases of the problems which are most likely to be difficult to overcome in an electrically actuated brake. The requirements set forth below are divided into two groups, the first dealing with force and torque, and the second dealing with operating characteristics.

1. Force and torque requirements. The brake must produce enough tangential force at the mean friction radius to give a torque of 0.55 WR where W is the weight of the airplane divided by the number of brakes per airplane, and R is the loaded radius of the tire. Usually, the mean friction radius is between 0.3 and 0.5 of the loaded radius of the tire. The normal force required between the friction surfaces will vary inversely with the coefficients of friction of the pairs of material used. For the bimetallic friction pairs used in multiple disk brakes, the coefficient ranges from 0.1 to 0.2, and for molded lining material against chrome plated steel or cast iron as used in drum and single disk brakes, the coefficient ranges from 0.2 to 0.3. These coefficient ranges are determined through experience to be found in the friction material pairs most satisfactory from a wear and durability standpoint. Of course, the force required will also vary inversely with the number of friction surfaces engaging under the same force.

2. One of the major difficulties of an electric control system is to obtain proper load feel comparable to manual operation. This would include all of the following points:

(*a*). Brake torque should be proportional to pedal pressure. This should be true regardless of whether the brake operation is going on or off.

(*b*). Brake operation should go from full- on to full-off position or vice versa with no appreciable time lag.

(*c*). Brake operation must be capable of small changes of torque in either direction without time lag and without returning to brake-off position.

(*d*). The brake must be capable of being held at constant torque during the period of stop.

(*e*). Some means of parking the brake for long periods of time must be provided.

In the design of an electric brake-operating system, it must be remembered that the weight and the space used should be reasonably close to the weight and space requirements of an equivalent hydraulic system. This, of course, applies to the entire system, from the pilot's control down to the brake itself.

Another big problem which is rolling up over the horizon is the prerotation of wheels prior to landing. This is desired to avoid unusual wear of tires upon impact with hard-surfaced runways. It will not require much power, as ample time can be allowed to bring the wheel up to speed. If the electrical engineer can solve the brake problem in less space and weight than is now required for the hydraulic system, there may be space enough for a small electric motor to deliver prerotation of the wheels.

The problems of controlling an airplane, its engines, and some of its accessories increase with aircraft size and again with altitude. For example, it is possible now to build airplanes so large that human effort cannot control them. Power boost is needed. It can be hydraulic or it can be electrical.

At high altitudes pressurized cabins will be a necessity. To operate cables or push rods through the walls of a pressur-

ized cabin requires meticulous care in design, installation, and maintenance. Rotational couplings at the cabin walls are only slightly better. Electric equipment is ideally suited for the solution of this problem.

Automatic controls for flight, engines, engine accessories, heating systems, ventilating systems, and other aircraft luxuries will be a big advantage to the airplane crew. Again electric apparatus is ideally suited for these applications.

Electric Propulsion of Aircraft

The biggest task that is just peeping over the horizon is an electric drive for the airplane. In other words, this means an electric coupling of generator and motor between the power plant and propellers.

Assuming that weight can be kept within reasonable limits, there are a number of advantages favorable to the use of electric-propulsion equipment on multiengine airplanes.

One of the largest single elements of drag to be overcome in propelling the conventional airplane through the air is due to nacelle and cooling losses, with engines in the leading edges of the wings or in the fuselage nose; this element of drag may amount to from 20 per cent to 40 per cent of the total horsepower requirement of the engines. Because of the relatively small size of an electric motor compared with engines, propellers may be placed where the designer wishes, submerging the motors entirely within the wing or other structural element without recourse to drag-inducing nacelles. In some instances, the weight saving due to the reduced fuel load when nacelles are eliminated may be sufficient to offset the increase in power-plant weight due to the addition of the electric transmission.

With electric drive, the prime movers and generators are located in the fuselage instead of in the wings. In land planes, it may sometimes be necessary to employ a somewhat larger fuselage than normal to accommodate the engines; in the case of sea planes; there is always sufficient space in the hull near or below the water line, a preferred location. With the engines in locations readily accessible to the crew, flight operations may be carried out with standby engines disconnected and all propeller motors running in parallel from a common power bus; and when necessary, simple repairs may be effected on standby units in flight.

Parallel operation of motors will eliminate the entire synchronization problem; and the removal of heavy-overhung engine mounts from wings should permit desirable structural redesign with over-all improvements in structure weights. The concentration of the major mass of the prime mover in the fuselage near the center of gravity of the airplane increases maneuverability and decreases the required control-surface areas; it further provides a convenient recoil-absorbing mass to take the recoil from heavy machine guns and cannon in military aircraft, without the necessity of shooting through a propeller.

Separation of engines from wing structures and the possibility of employing effective shock-absorbing engine mounts may also lead to structural weight savings. Breaking up the motor drives into a large number of lower-power units will also be advantageous in this case, and will permit using small lightweight two- and three-blade propellers with the higher efficiencies inherent in such propellers when over-all design permits, instead of using lower-efficiency three- and four-blade propellers of large power ratings.

A further advantage is the possibility of employing pusher-type propellers instead of tractors, since motors may be sunk in the wings. The increased lift and reduced drag over wing sections in the vicinity of propellers are pronounced at high speeds when propellers are placed behind the wing rather than in front of the wing, thereby reducing turbulence due to the propeller slip stream over the wing.

On the other hand, an electric drive will result in increased power-plant weight, lower efficiency between fuel tank and propeller shaft, increased cost of power plant, and with gasoline-engine prime movers, probably no reduction in maintenance. Further, the installation of engines and alternators in the fuselage of a bomber or cargo airplane may result in disadvantageous bomb or cargo location.

With the above features established, it appears that the most advantageous system would require the use of high-speed high-frequency induction motors and corresponding synchronous generators. Estimates have been made based upon three-phase induction motors to operate in the 400–500-cycle range at a transmission voltage of the order of 1,000 volts, and with turbo-type alternators to furnish power for the motors.

Preliminary analyses have been made and it is believed to be practical to produce an electric coupling, alternator, motor, and controls at the following weights:

Horsepower	Pounds	Pounds Per Horsepower
1,000	750–850	0.85
2,000	1,200–1,300	0.65
5,000	2,300–2,700	0.54

At each of these ratings the alternator and motor weights will be divided about in the ratio of 60 and 40 if both can operate at approximately the same speed.

The probable advent of high-speed internal-combustion turbines will greatly improve the case for electric drives. The weight and the efficiency loss of the coupling are handicaps and there must be two gains to compensate for them:

1. Compensating reductions in fixed weight must be made in the power plant, propellers, structure, and accessories.

2. The over-all efficiency of the airplane must be improved by eliminating aerodynamic drag to compensate for the lowered efficiency of the coupling.

Storage of Electric Energy

The storage battery is another problem. Perhaps there is no answer, but even with aircraft designs of lead-acid storage batteries, the storage of a kilowatt-hour of energy costs approximately 100 pounds. The same available energy is stored in 0.25 pound of gasoline. Both represent chemical storage of energy.

The electric motor which converts electric energy to mechanical energy weighs less at equal ratings than does the internal-combustion engine which converts gasoline to mechanical energy. The possibilities inherent in the storage of electric energy at 0.25 pound per kilowatt-hour are beyond the horizon and beyond the imagination.

Harnessing of atomic fuel is the most distant goal and it is quite likely that the clue to storage of electric energy at less than 0.25 pound per kilowatt-hour lies at that goal.

The Final Problem

A less tangible horizon is that of public confidence. The performance of structural materials can be predicted, duplicated, and set up in tables. This is not always true of electric apparatus in aircraft. Most people employed in aviation are mechanically minded. They do not understand electricity. To them it is a mysterious phenomenon. They are afraid of it. The news of a failure of an electrical item spreads through the industry, military services, and airline operators at an unbelievable rate. Once spread, all efforts to educate personnel will not eradicate the rumor. Further it is difficult to revise the incorrect operating procedure that may have gone with the failure.

In short, we in the electrical industry must live together. We cannot ignore the troubles of others. The fault of one piece of electric equipment is a discredit to the whole system. D'Artagnan's motto for the three musketeers can well be a slogan for every aviation electrical engineer.

High-Altitude Brush Problem

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THE rapid development of military aircraft during the past three years has been accompanied by an equally vigorous development and use of electric motors and generators necessary for their operation and control. This dynamoelectric equipment covers not only the d-c and a-c applications but also a wide range of power output, voltage, current, and speed. Some of the d-c machines are compensated fully for best commutation, while others are partially compensated, and still others are without compensation and depend on the brush shift and brush grade for satisfactory operation.

The development of carbon and metal-graphite brushes likewise had to keep pace with the development of the electric equipment. At first it seemed that the demand could be met easily, and many standard grades of brushes were used with satisfactory results as long as the planes were flying at low altitudes. However, when the airplanes began to fly at altitudes of 20,000 to 40,000 feet the brushes in many instances were worn out in one to three hours. It became necessary first to determine the causes of the rapid brush wear, and then either to develop treatments for the protection of the existing brush grades, or to make a series of entirely new brush grades capable of withstanding the severe conditions imposed upon them by high-altitude flying, as well as being capable of performing satisfactorily at sea level.

Rapid Brush Wear on Pursuit Planes

In December 1940 a testing program was started in the National Carbon Company laboratory to determine the cause of rapid brush wear encountered in actual flights. The brushes in question are in the motor circuit used for controlling the pitch of the Curtiss variable-pitch propeller. The controlling motor is mounted in the nose or hub of the propeller, which in turn operates the planetary-gear mechanism connected to the blades. The d-c motor is energized through four slip rings which are mounted on the propeller shaft and insulated from it. A stationary brush holder, which is mounted on top of the protective housing, completes the electric circuit to the rings through four rows of small brushes. These are the brushes which were showing rapid wear during flights.

For the first approach of the problem the testing was conducted with equipment made from an actual propeller brush housing and holder (Figure 1). By these means it soon was established that brush temperature and current density had but small effect on brush wear. However, when air with a moisture content of less than 0.15 grain per cubic foot was introduced into the brush housing, rapid brush wear invariably took place after 5 to 30 minutes. Under these conditions the operation of the brushes was accompanied by a loud grinding noise, and a cloud of brush dust could be seen rising from the top of the brush holder. The brushes actually were ground to dust, and in 30 to 60 minutes all of the available brush length was gone.

To meet this problem, two organic lubricants, "*A*" and "12," were developed to be used in conjunction with grade 840-*K* (a copper-graphite material) which was then standard on this equipment. The brushes so treated were designated as *A*-840*K* and 840*K*-12. These two organic treatments provided boundary lubrication on the propeller rings and protected the brushes from rapid wear throughout their life in dry-air atmosphere.

The laboratory findings were checked in actual airplane flights.* For this purpose 15 pursuit ships were used, some of which were equipped with grade 840*K* and the remainder with *A*-840*K* and 840*K*-12. Seventeen sets of 840*K* brushes were worn out, some of them within two hours of flying time; whereas grades *A*-840*K* and 840*K*-12 operated satisfactorily. In one instance grade *A*-840*K* was tested for 112 hours of flying time, and the brushes were not worn out.

This was our first experience with rapid brush wear encountered on military planes. The quick solution provided by the introduction of grades *A*-840*K* and 840*K*-12 was only the beginning of the intensive research which was to follow in the study of the high-altitude performance of carbon and metal-graphite brushes. In the remainder of this paper is given the description of the test equipment used, the results obtained, the theoretical evaluation of the high-altitude problem, and finally its present solution.

* The results of these test flights were given to us through the courtesy of M. E. Cushman, engineering department, Curtiss Wright Corporation, Newark, N. J.



Figure 1. Brush-testing unit for Curtiss variable-pitch propeller

The Atmosphere at High Altitudes

The composition of pure dry air by volume is 78 per cent nitrogen, 21 per cent oxygen, and 1 per cent of ten other gases.[1] These proportions have been found to be essentially the same irrespective of location and for altitudes up to 20 miles. As the altitude is increased there is rapid drop in air pressure, temperature, and moisture content. This is clearly illustrated in Figure 2 in which curve *A* shows the change in air pressure with altitude and curve *B* the change in air temperature and dew point for 100 per cent relative humidity with altitude. The dotted curves C_1 and C_2 indicate respectively the highest and lowest dew-point temperatures for altitudes between 30,000 and 34,000 feet. The data for these two curves were obtained by actual dew-point measurements made in flights at Patterson Field by the United States Air Corps.

In Figure 3 curves *A*, *B*, and *C* show respectively the partial pressure of nitrogen, oxygen, and saturated water vapor with altitude. Curve *D* represents the variation of moisture content of saturated air with altitude. The data for this curve were based on the temperature–altitude curve *B* of Figure 2. At 34,000-feet elevation the air pressure is one fourth of the sea-level pressure, and for this reason the partial pressure of oxygen is reduced by the same factor. The moisture content of saturated air from curve *D* corresponding

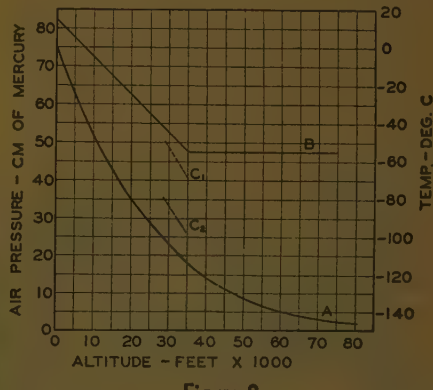


Figure 2

A. Air pressure versus altitude
B. Air temperature versus altitude

Curves *A* and *B* are based on National Advisory Committee for Aeronautics data. Curves C_1 and C_2 are highest and lowest dew-point temperatures measured at Patterson Field

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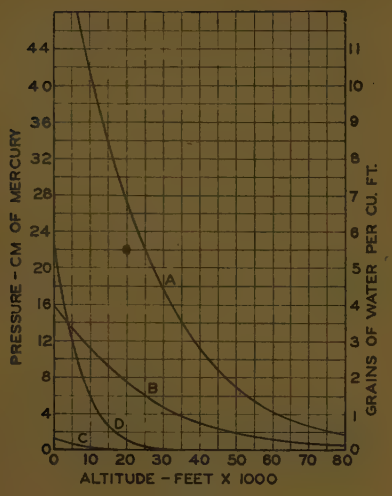


Figure 3

A, *B*, and *C* represent respectively the partial pressures with altitude of nitrogen, oxygen, and saturated aqueous vapor. *D* is the weight in grains of a cubic foot of saturated aqueous vapor versus altitude. All curves are based on National Advisory Committee for Aeronautics data

to 34,000 feet is 0.015 grain per cubic foot. To complete our picture of the atmosphere at high altitudes, that is, 30,000 to 50,000 feet, it may not be out of place to mention that the ultraviolet rays are strong enough to cause the formation of small amounts of ozone and nitrous oxides.

Experimental

Vacuum Chambers

For the study of the operating properties of carbon brushes at reduced pressures and in various atmospheres, two vacuum chambers were used. Both chambers and the auxiliary equipment are shown in Figure 4. Since the two chambers are practically identical in construction it will be sufficient to describe the one on the left which is visible in greater detail.

The vacuum chamber consists of a glass cylinder which is sealed with rubber gaskets between two steel plates. Inside the chamber on a shaft protruding through the supporting back steel plate is mounted a slotted copper ring. This ring, which is insulated from the shaft, is often referred to as a "dummy commutator." There are four radial brush holders symmetrically spaced around the ring. The brush size is two by three-fourths by three-eighths inches. The brush-spring tension is adjustable and can be varied from 0 to 15 pounds per square inch. The ring is 6.125 inches in diameter and is driven by a variable-speed d-c motor.

The outside air is kept from entering the chamber by a special seal on the shaft. A low-voltage d-c source provides the electric current which flows between the brushes and the dummy commutator. The chamber can be closed and pumped to a pressure of 0.5 millimeter of mercury in five to ten minutes. The pressure then can be regulated by a needle valve which controls the flow of dry air into the chamber. The dew point of the dry air used is below −70 degrees centigrade. If desired other gases than air may be allowed to enter the chamber.

The brush temperature is measured by a potentiometer connected to thermocouples inserted in the brushes close to the rubbing surface.

Brush Wear in Various Gases at Atmospheric Pressure

It was already mentioned that carbon or metal-graphite brushes can be made to wear rapidly or dust at atmospheric pressure if the surrounding air does not contain over 0.15 grain of water per cubic foot of air. Such rapid wear in dry air can be stopped either by increasing its moisture content or by increasing its oxygen content over the amount normally contained in the atmosphere.

Brushes wear rapidly also if the surrounding atmosphere is made up of any of the following gases in dry form: nitrogen, hydrogen, carbon dioxide, and argon. Rapid brush wear or dusting does not take place in ordinary room air mixed either with ozone gas or carbon tetrachloride vapor, although the latter does double the brush friction. In general, contaminants present in the atmosphere in the form of fumes and vapors will tend to prevent rather than bring about rapid brush wear.

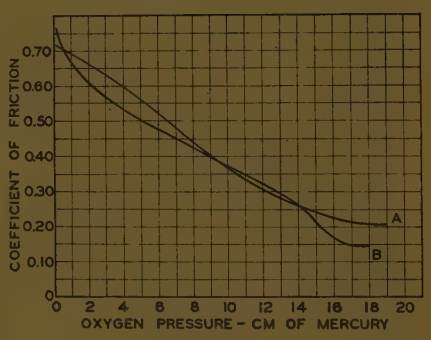


Figure 5

A. Coefficient of friction versus oxygen pressure for graphitized carbon brushes

B. Coefficient of friction versus oxygen pressure for carbon–copper brushes

Brush-spring pressure—11.0 pounds per square inch

Effect of Oxygen, Water Vapor, and Temperature on the Coefficient of Friction and Wear of Brushes

For the experiments with oxygen and water-vapor atmospheres a uniform technique was used. Before each experiment the brushes were heated to a temperature of 300 to 350 degrees centigrade to free them from absorbed moisture. The commutator was heated to 135 degrees centigrade and then quickly resurfaced with a fine-grained commutator stone. The dust was blown away, the brushes were installed, and the chamber was closed. In about ten minutes the chamber was evacuated to a pressure of one millimeter of mercury.

In the experiments with oxygen a steady flow of dry oxygen with a dew point below −40 degrees centigrade was maintained. The rate of flow was limited only by the capacity of the pump at the pressure desired. When water vapor was used the chamber first was evacuated and then water vapor admitted from an evacuated flask. One straight and one inclined mercury gauge were used to

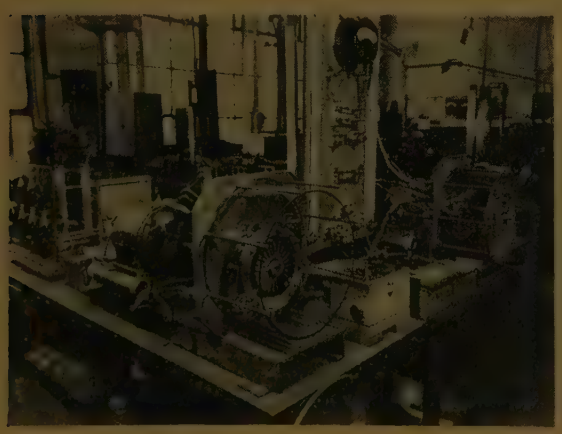


Figure 4. Vacuum chambers used to study the operating characteristics of carbon and metal-graphite brushes at reduced pressures and in various atmospheres

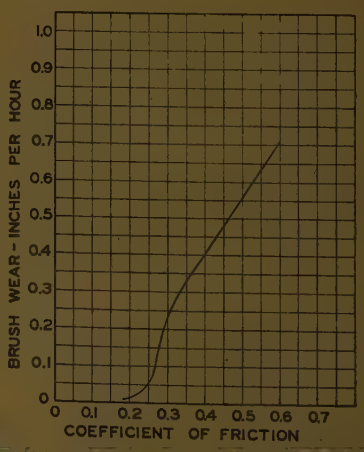


Figure 6. Brush wear versus coefficient of friction for graphitized carbon brushes in dry oxygen atmosphere

Brush-spring pressure—11.0 pounds per square inch

measure the pressure in the chamber.

For the brush-wear measurements a gauge accurate to one-ten-thousandth of an inch was used. Brush-wear measurements were found to be consistent and easy to reproduce. All of the data plotted in Figures 5 to 9 were obtained with a brush-spring pressure of 11 pounds per square inch.

The speed of the dummy commutator purposely was kept low and constant at 1,000 rpm, giving a rubbing speed of 1,600 feet per minute. The driving d-c shunt motor was connected to a constant voltage supply. This made it possible to maintain constant field excitation and use the armature current for measurement of friction.

For the experiments in oxygen and water-vapor atmospheres, two kinds of brushes were used. One was a graphitized carbon grade and the other a carbon grade containing 50 per cent copper. The data for all curves were obtained without the flow of current through the brushes. Brush wear and frictional measurements in an oxygen atmosphere present explosion hazards if electric current is permitted to flow from brushes to commutator.

Tests in Dry Oxygen Atmosphere. The relation between coefficient of friction and oxygen pressure for graphitized carbon and carbon–copper brushes is shown in Figure 5. The differences in coefficient of friction for a given oxygen pressure between curves *A* and *B* are small. The final value of the coefficient of friction for the carbon–copper brushes, curve *B*, is lower than that of the graphitized carbon brushes.

The curves *A* and *B* in Figure 5 would be only of academic value if the relationship between the coefficient of friction and brush wear were not known. This relationship is shown in Figure 6 which is for graphitized carbon brushes. A similar relationship exists for the carbon–copper brushes. The data for this curve were obtained by measuring brush wear and coefficient of friction for different oxygen pressures in the chamber and at as nearly constant brush temperatures as it was possible to obtain. For values of the coefficient of friction between 0.3 and 0.6 brush wear is directly proportional to it. When the coefficient of friction is less than 0.3 brush wear decreases at a faster rate and becomes normal with a coefficient of 0.17. This curve shows further that brush-wear values are large, particularly if one bears in mind that the available brush length for wear in actual practice is one-fourth to three-eighths inch.

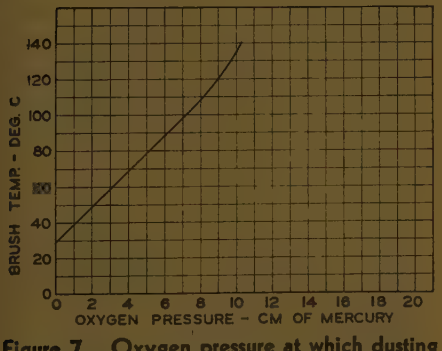


Figure 7. Oxygen pressure at which dusting or rapid wear begins versus brush temperature for graphitized carbon brushes

Brush-spring pressure—11.0 pounds per square inch

Of interest in the experiments with oxygen atmosphere is the relationship between the brush temperature and the oxygen pressure at which rapid brush wear begins.

The curve in Figure 7 shows that at an oxygen pressure greater than 1.1 centimeters of mercury rapid brush wear will not begin with a brush temperature of 40 degrees centigrade. When the brush temperature reaches 130 degrees centigrade an oxygen pressure greater than 9.7 centimeters of mercury is necessary to prevent rapid brush wear. A brush temperature of 130 degrees centigrade corresponds to a commutator temperature of about 100 degrees centigrade. The apparent discrepancy in the amount of oxygen pressure necessary to stop rapid brush wear at the two widely different brush temperatures is undoubtedly due

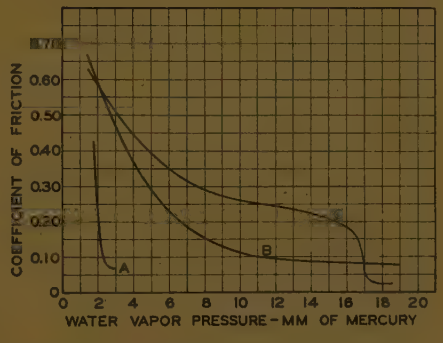


Figure 8. Coefficient of friction versus vapor pressure of water

A. Average brush temperature—60 degrees centigrade

B. Average brush temperature—135 degrees centigrade

C. Average brush temperature—115 degrees centigrade

A and *B* are for graphitized carbon; *C* is for carbon–copper. Brush-spring pressure—11.0 pounds per square inch

to the different amounts of condensed water vapor on the commutator surface. When the brush temperature increases beyond 130 degrees centigrade any condensed water vapor is quickly removed from the commutator surface, and the oxygen pressure necessary to prevent rapid brush wear or dusting approaches a constant value.

The data for this curve were obtained in the following manner: With the brushes and commutator at room temperature, the vacuum chamber was pumped to a pressure of one millimeter of mercury and the commutator brought up to 1,000 rpm. Immediately the brushes began to wear rapidly or dust. Since the brush friction under these conditions was large, the brushes and commutator began to heat up quickly. The commutator temperature was lagging behind the brush temperature about 25 to 30 degrees centigrade.

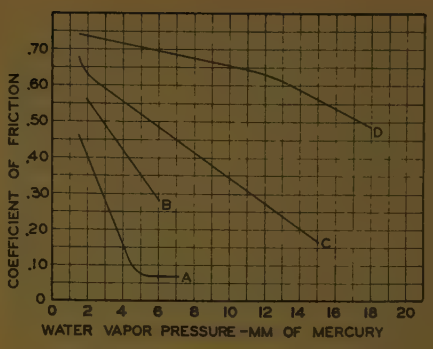


Figure 9. Coefficient of friction versus water-vapor pressure

A. Average brush temperature of 80 degrees centigrade

B. Average brush temperature of 110 degrees centigrade

C. Average brush temperature of 140 degrees centigrade

D. Average brush temperature of 170 degrees centigrade

A, *B*, *C*, and *D* are for carbon–copper brushes

As soon as the brushes began to dust enough oxygen was allowed in the chamber to stop the rapid wear. At this point the oxygen flow was interrupted while the pump was gradually reducing the oxygen pressure in the chamber. When rapid brush wear or dusting began once more the oxygen pressure and brush temperature were recorded. Again enough oxygen was admitted into the chamber to stop the rapid brush wear, and the cycle was repeated. With each succeeding cycle the brush and commutator temperature increased, and thus it was possible to obtain the data of brush temperature and oxygen pressure at which rapid brush wear began.

Test in Water-Vapor Atmosphere. In water vapor the differences in coefficient of friction for carbon and carbon–copper brushes are more pronounced than in oxygen atmosphere. In Figure 8, curves *A* and *B* show the relationship of coefficient of friction versus vapor pressure of water for graphitized carbon brushes. Curve *C* is for carbon–copper brushes.

Curve *A* was obtained with a brush temperature of 60 degrees centigrade. In this case a pressure of water vapor of 2.9 millimeters of mercury is sufficient to lower the coefficient of friction and brush wear to normal. Curve *B* was obtained with a brush temperature of 135 degrees centigrade and requires 19 millimeters of mercury of water vapor to bring the

coefficient of friction and brush wear to normal. Curve *C* was obtained with an average brush temperature of 115 degrees centigrade which is lower than the temperature for curve *B*. Nevertheless, for the range of water-vapor pressures of 2 to 17 millimeters of mercury, the coefficient of friction of curve *C* is greater than that of curve *B*. The reason for that difference is undoubtedly due to the copper contained in the brush which penetrates through the water film more readily.

The effect of brush temperature on the coefficient of friction of carbon–copper brushes in water vapor is perhaps better illustrated in Figure 9. Curves *A*, *B*, *C*, and *D* were obtained with the respective brush temperatures of 80, 110, 140, and 170 degrees centigrade. With a brush temperature of 170 degrees centigrade, 18 millimeters of mercury of water vapor had but little effect on the coefficient of friction and rapid wear of the brushes.

Discussion of Results. The effect of oxygen on the coefficient of friction of nickel sliding on tungsten has been reported by Bowden and Hughes.[2] They found that oxygen at a pressure of 10^{-3} millimeters of mercury was sufficient to lower the coefficient of friction for nickel sliding on tungsten from 6.0 to 1.6. In this instance an attempt was made to obtain photoelectrically clean surfaces.

Experiments with photoelectrically clean carbon sliding on copper will be very difficult to perform. Carbon is heavily loaded with gas. Furthermore, some provision will have to be made to bake completely the chamber, copper commutator, and brushes under good vacuum at a temperature of 500 to 600 degrees centigrade to drive out the adsorbed moisture. The problem, however, is very much simplified by the rapid wear of the brushes. After the brushes have been heated to a temperature of 300 to 350 degrees centigrade and the commutator to a temperature of 150 degrees centigrade or higher, which is sufficient to drive off absorbed moisture, the adsorbed moisture and gases easily can be removed in vacuum as soon as the brushes begin to wear rapidly. Under such conditions films of contamination which might be on the commutator surface are removed in a few seconds. The flow of oxygen which is always maintained in the chamber helps to sweep away contaminants which otherwise may be present.

There is some evidence to show that the oxygen gas protects the sliding surfaces of brushes and commutator from rapid wear, not only by adsorption but also by oxidation.

Water vapor is known to be a good boundary lubricant. On stationary surfaces adsorbed water films are difficult to drive off. On a sliding surface such as a commutator surface, particularly when its temperature is above 100 degrees centigrade, the adsorbed water films can be removed by the brushes in a few seconds.

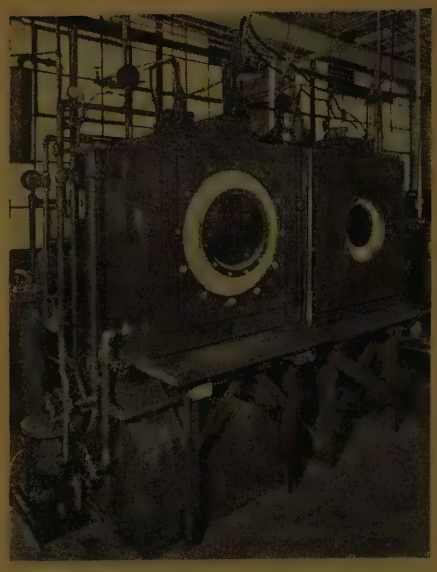


Figure 10. High-altitude chambers used for testing of Amplidynes, motor alternators, inductor alternators, inverters, dynamotors, and other dynamoelectric machines of the motor generator type

Water is ten times as effective as oxygen in reducing the coefficient of friction and preventing rapid brush wear. In general, oxygen pressure of 180 millimeters of mercury is sufficient to prevent rapid brush wear, while water vapor at 18 millimeters of mercury will be equally effective.

Water vapor is most effective on a cold commutator. With graphitized carbon brushes and brush temperature of 60 degrees centigrade water vapor at a pressure of 3 millimeters of mercury is equally effective as water vapor at a pressure of 19 millimeters of mercury when the brush temperature is 135 degrees centigrade. The commutator temperatures are lower than the respective brush temperatures.

As stated before, the data for the curves in Figures 5, 6, 7, 8, and 9 were obtained with two kinds of brushes: one a graphitized carbon grade, and the other a carbon grade containing 50 per cent copper. While the coefficient of friction and rate of wear will be different for brushes of different composition and processing, all brush grades which are not high-altitude type have one thing in common; namely, they wear rapidly, and the coefficient of friction is high under the test conditions previously described.

The Commutator Film

From the description which has been given of the experiments, it is evident that carbon or carbon–copper brushes will operate on a copper commutator with small brush and commutator wear only if there is a film of separation between the rubbing surfaces. This film consists of copper oxide, carbon, adsorbed oxygen, adsorbed water, and contaminants from the air. The contaminants often can be of organic nature such as oil vapors. Once this film is removed the rubbing surfaces of brushes and commutator seize, and quick abrasion of both takes place.

From the fundamental studies of W. B. Hardy[3] we know that when two clean surfaces are brought together within a very close range actual seizure or welding takes place at the points of contact. This seizure is caused by the powerful attractive forces due to the surface atoms. It is further known that these attractive forces are effective only within a very short range, and therefore a boundary film of separation is sufficient to reduce their value to the point where actual welding at the points of contact will not take place. Thus, the whole problem of satisfactory brush operation is reduced to that of boundary lubrication.

At sea level with normal humidity a boundary lubricating film of adsorbed moisture and oxygen is constantly covering the commutator surface. In extremely dry weather, at either sea level or high altitudes, this is no longer possible, and rapid brush wear may follow.

The boundary film on the commutator surface can be organic or inorganic. This film will have to be deposited by some means on the commutator surface throughout the life of the brushes. In our solution of the problem it was found best to let the brushes deposit the film. This means that the brushes for high-altitude applications must be processed specially.

In the use of organic films the treatments or processes known as "*A*" and "12" were already mentioned. These two treatments were developed for use on rings. For brushes used on commutators a complex organic lubricant was developed which was designated as "21." This lubricant has low vapor pressure at temperatures as high as 200 degrees centigrade; it is resistant to oxidation; and, what is most important, it provides a film which gives satisfactory brush life, contact drop, and coefficient of friction under high-altitude conditions. This film also does not introduce periodic contact drop fluctuations frequently referred to as cycling. The "21" lubricant operates best with metal-graphite and resin-bonded natural-graphite brushes.

For the deposition of inorganic protective films a whole series of new grades has been developed ranging from electro-

Table I

Grade	Per Cent Copper Content	Resistivity in Ohm-Inch
ANK	75	0.0000091
ALP	65	0.000019
AJT	45	0.00035
AJZ	25	0.00055
AJS	0	0.0023
AMY	0	0.00040
AMZ	0	0.0025

graphitic brushes to metal-graphite brushes containing more than 70 per cent copper. Some of the brush grades developed for high-altitude applications are given in Table I. The operating temperatures which these grades will withstand are limited not by the brushes but by the machines themselves.

Performance of Grade *AJT* in the Vacuum Chamber

Grade *AJT* is a copper-graphite grade containing 45 per cent copper. It was selected as an example because it is typical of all grades with the fine protection it has against rapid wear at reduced pressures. Table II gives brush life and the coefficient of friction in room air at about 50 per cent humidity, room air at 100 per cent humidity, dry air at atmospheric pressure, dry air at 18.2 centimeters of mercury, and vacuum at 0.1 centimeter of mercury.

The amount of copper used in the various grades listed in Table I has a direct effect on the contact drop with which they will operate at a given current density. Excellent brush life, however, has been obtained with all grades under similar test conditions as in Table II.

The High-Altitude Chamber

So far this paper has presented the operation of brushes on copper rings. The final test had to be made on actual electric machines at air pressure, temperature, and dew-point conditions simulating those at high altitudes.

The general appearance of the two high-altitude chambers, which were constructed for tests of dynamoelectric equipment, is shown in Figure 10. The chamber on the left has its front insulating cover removed in order to show the bolted head which closes the chamber. These two chambers which are alike have worked so well from the time of their construction that a brief description may not be out of place for the sake of those who might be interested.

The chamber was constructed from steel pipe 16 inches in diameter and 18 inches long. One end of the pipe is closed by a welded steel plate, and the other end is welded to a flange to which the front cover is bolted. To increase the cooling surface of the chamber on the inside, longitudinal radial steel fins have been welded around the periphery. The chamber is jacketed with a cylindrical steel case forming an annular space for holding a mixture of dry ice and high-boiling-point naphtha. The steel chamber is housed in a wooden box with four inches of heat-insulating material between the steel jacket and the box walls. It is important that the chambers be well constructed to prevent any leakage of room air which would cause frost deposition in the chamber and lead to erratic results.

Under all conditions of operation the chambers are constantly flushed with filtered dry air, the dew point of which is −70 degrees centigrade. This air is never recirculated. The amount of dry air admitted is limited by the capacity of the pump at the pressure at which the chambers are operated. At an air pressure of 19 centimeters of mercury which corresponds to an altitude of 34,000 feet, the air in the chamber is changed 10 to 20 times per hour. At no time is there any frost visible on the inside walls of the chambers. The importance of taking this precaution hardly can be overstressed. This is undoubtedly one of the reasons why test results obtained have always been in close agreement with actual airplane tests made at Patterson Field.

The walls of the chambers are maintained at a temperature of −70 degrees centigrade, although the inside air temperature may rise up to −50 or −40 degrees centigrade, depending on the amount of heat dissipated by the unit under test. The surrounding cold walls help to freeze the impurities which might be given off by the windings of the machines under test. Most of these impurities naturally are swept out by the dry air which flushes the chambers. The low wall temperature also provides a continuous check of the dew point of the air entering the chamber. As long as there is no frost deposited on the chamber walls it is certain that the dew point of the air is lower than −70 degrees centigrade.

Table II

Grade *AJT*

Hours Per Inch	Coefficient of Friction	Atmosphere
6,000	0.21	Room air, 50 per cent humidity
38,000	0.25	Room air, 100 per cent humidity
1,200	0.18	Dry air, sea level
1,500	0.19	Dry air, 18.2 centimeters mercury
25,000	0.12	Vacuum, 0.1 centimeter mercury

Tests in High-Altitude Chambers

The machines tested in the high-altitude chambers cover a wide variety of types and makes. These included Amplidynes, motor alternators, inverters, dynamotors, inductor alternators, and various other types of small machines. The procedure generally used was to operate the machines in room air with the brushes with which they were received and check their input and output ratings. The length of the room air test varied from 24 hours to several days. After this they were tested in the chambers under the high-altitude conditions as described in the preceding section. The load conditions were varied for the different machines. To simulate the actual working load, the Amplidynes were operated under a cyclic load schedule of 10 minutes full load and 50 minutes no load each hour, while the dynamotors carried a full load continuously. On the other hand, it was necessary to operate the inverters and alternators at 50 to 75 per cent of their sea-level rating to prevent excessive heating under the high-altitude conditions. In many instances when the machines were supplied with brushes which had little or no high-altitude protection the brushes wore out in one to five hours. After this the commutators were resurfaced, high-altitude brushes installed, and the tests repeated. Table III gives the life obtained with ordinary and with high-altitude brushes on these machines in the high-altitude chambers.

With some small machines, the output current of which is only a few milliamperes, the commutators and brushes operate at temperatures considerably lower than 100 degrees centigrade. The brushes of such machines even if they do not have high altitude protection may not wear rapidly for several hours or even days because of the effect of condensed moisture on the commutator and quite often because of the condensation of contaminants given off by the windings of the machine.

Effect of Sparking on Brush Life

In the introduction it was mentioned that the machines used today on military airplanes can be divided from the point of view of compensation into three general groups: completely compensated, partially compensated, and without compensation. This has made it necessary to consider carefully the selection of proper brush grades for satisfactory commutation at sea level. Machines without compensation usually use carbon brushes with or without a small amount of copper.

Table III

		High-Altitude Brushes		Ordinary Brushes	
	Brush Grade	Brush Life in Hours for 1/4-Inch Wear		Brush Life in Hours for 1/4-Inch Wear	
Amplidyne					
Input	*AJT*	250		4	
Output	*AJT*	300		4	
Dynamotor					
Input	*AJT*	1,100		12	
Low-voltage output	*ALP*	960		19	
Medium-voltage output	*AJS*	2,100		162	
High-voltage output	*AJS*	5,500		119	
Inverter					
Input	*AMZ*	1,200		0.9	
Inverter					
Input	*AJZ*	214		0.5	
Output	*AJT*	346		130	
Alternator					
Input	*T-171*	240		0.3	
Rings	*AME*	1,350		440	
Inductor Alternator					
Input	*AJZ*	520		6	

Instrumentation of 400-Cycle Aircraft Electric Systems

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THE use of 400-cycle alternating current[1] as a power source on large aircraft originates the problem of adequate instrumentation at the distribution panel. Load conditions on a d-c system are determined by the simple parameters of voltage and current, whereas those of the a-c system include frequency and active and reactive power, in addition to voltage and current. Specifically, the flight engineer will require the following information for intelligent operation of the system:

1. Voltage indication, to show whether or not a machine is operating, and as a guide in the adjustment of the voltage regulator.

2. Current indication, to determine whether or not a machine is overloaded. (Overload also can be detected from voltmeter and watt-varmeter indications, but this requires considerable mental effort on the part of the flight engineer.)

3. Active- and reactive-power indication (where two or more generators are operated in parallel), to guide the adjustment of governors for active load division and adjustment of regulators for reactive load division.

4. Frequency indication, to guide the remote adjustment of governors for frequency level where provision for such adjustment has been made.

To summarize, a group of four instruments, namely, ammeter, voltmeter, watt-varmeter, and frequency indicator, together with their associated resistors and current transformer, is desired for use in a-c systems where parallel operation of several alternators is required. Where nonparallel operation is used, either the watt-varmeter or ammeter can be eliminated. For installations using several alternators, duplication of certain instruments such as voltmeters and frequency instruments can be avoided by use of transfer switches. Normally it is expected that it will be necessary to read only one phase voltage, current, and power in each machine. This group of instruments will give the flight engineer reasonably complete knowledge of circuit conditions. In addition to their specific uses, they aid in the location and correction of minor faults and thus prevent major ones. They serve the flight engineer in much the same manner as the flight instruments serve the pilot.

General Requirements—What Size and Weight?

To be specific, we will consider the distribution system of a three-phase 208-wye/120-volt 30-kw 40-kva 400-cycle alternator and determine the minimum size and weight which will characterize a satisfactory group of instruments. All instruments will have a body diameter of $2^1/_4$ inches, standard aircraft flange-mounting facilities, with depths behind the panel to suit the individual elements. All instruments will have scale lengths of approximately 90 angular degrees and a general accuracy rating of two per cent of full-scale value. They must be designed to withstand satisfactorily the vibration, changes in temperature, air density, and humidity which may be encountered in service. Reasonable independence from load conditions, excepting the particular condition under measurement, is also essential. This may be described as the *self-isolating* property of an instrument by which wattmeter indications are unaffected by frequency change, frequency-meter indications are unaffected by vibration or voltage change, and so forth. These, in brief, are the general requirements on which the designs were based.

Measurement of Current and Voltages

Small-panel a-c moving-iron[2] voltmeters and ammeters have been available from American manufacturers for some years. The investigational work thus was limited to the selection of a mechanism of suitable size, as shown in Figure 1, its enclosure in the standard $2^1/_4$-inch case, and determination of its characteristics in relation to the requirements. The instrument constants and conditional errors, as shown in Table I, are believed to be consistent with an accuracy rating of two per cent of the full-scale reading.

Instrument Current Transformer

The continuous current rating of the 40-kva alternators is 111 amperes, and an ammeter-scale capacity of 250 amperes was chosen to provide liberally for overload conditions, which are of prime importance to the flight engineer.

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The authors acknowledge the assistance of L. G. Levoy, Jr., who supplied information on the functional use of the instruments.



Figure 1. Small-panel $2^1/_4$-inch moving-iron ammeter, cover removed

The partially or fully compensated machines often use copper-graphite brushes with a copper content of 50 per cent or more.

Electric machines which operate satisfactorily without sparking or with minute sparking at sea level may not necessarily perform the same way under high-altitude conditions. The reason for this is that the contact drop of the brushes decreases with a reduction of air pressure and may not be sufficient to prevent the flow of large circulating currents and intensive sparking. Since high-altitude brushes depend on their satisfactory performance on the boundary film deposited on the commutator, any injury caused to this film by intensive sparking naturally will reflect on brush wear and general brush performance. For this reason any compensation or machine adjustment which designers of electric machines can make to reduce or eliminate sparking will increase the likelihood of successful brush performance under high-altitude conditions.

References

1. METEOROLOGY FOR PILOTS, B. C. Haynes. United States Department of Commerce, Civil Aeronautics Bulletin 25.

2. THE FRICTION OF CLEAN METALS AND THE INFLUENCE OF ADSORBED GASES. THE TEMPERATURE COEFFICIENT OF FRICTION, F. P. Bowden, T. P. Hughes. Royal Society of London *Proceedings*, 172*A*, 1939, pages 263–79.

3. W. B. Hardy, J. K. Hardy. *Philosophical Magazine* and *Journal of Science*, volume 38, 1919, pages 32–48.

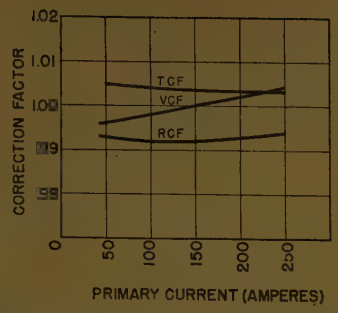


Figure 2. Curves showing correction factors for 400-cycle 125:1 current transformer for line power factor of 0.75, with burden of ammeter, watt–varmeter, and voltage regulator

Ratio correction factor (*RCF*) applies to ammeter reading. Transformer correction factor (*TCF*) applies to wattmeter reading. Varmeter correction factor (*VCF*) applies to var indication when the external resistor furnished with the watt–varmeter is used

To avoid the necessity of running power leads to the instruments and voltage regulator, and to make possible lightweight instrument designs, a special 125:1 current transformer of minimum weight was designed for this application. For simplicity of construction and economy of material, the window type was chosen. This type, with the current magnitudes involved, gave a low number of ampere turns which at 60 cycles would have required a rather heavy core. At 400 cycles, however, the flux required was low enough to permit use of a small core. Further advantage was obtained by using a high-permeability nickel-alloy core material wound in a continuous spiral so as to avoid joints. The secondary coil was wound toroidally on this core, thus practically eliminating any effect of leakage flux.

In order to achieve the minimum weight in this aircraft current transformer, it was designed and compensated for the particular burden with which it was to be used. Furthermore, the ratio and phase-angle errors were made largely to offset each other, thus giving a small



Figure 3. Window-type 400-cycle current transformer for aircraft, 2 1/2-inch diameter

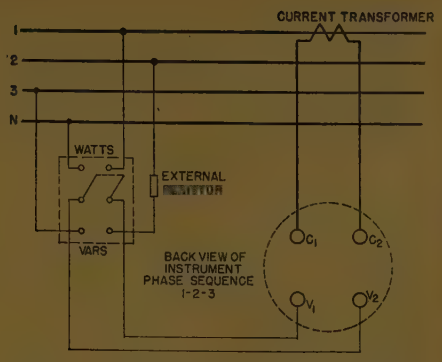


Figure 4. External-connection diagram for watt–varmeter

wattmeter error, even though the phase angle was larger than generally allowed in instrument transformers. These design principles permit the core to be of much smaller cross section than is the case when one transformer must be capable of supplying a range of burdens. Curves of ratio correction factor (*RCF*), transformer correction factor (*TCF*), and var correction factor (*VCF*) are shown in Figure 2. Of course, a transformer designed for a specific burden would be less accurate if used for other burdens, but that does not matter when the burden is definitely known in advance. Similar transformers for other burdens can be made readily by minor modifications of design.

The weight of the 400-cycle transformer, illustrated in Figure 3, is 0.43

Table I. Constants and Performance Data of 2 1/4-Inch 0–2.0-Ampere Ammeters and 0–250-Volt Voltmeters

Average of 16 Samples. Normal Accuracy—Two Per Cent of Full-Scale Value. Weight—0.37 Pound Each

	Ammeter	Voltmeter
Scale Constants		
1. Length (inches)	1.52	
2. Angle of deflection (degrees)	85.1	
Moving Element		
1. Full-scale torque (*T*) (millimeter grams)	0.24	
2. Mechanical factor of merit (*T/W*)	0.52	
3. Response time (seconds)	1.30	
Volt-Ampere Burden		
1. Voltmeter at 250 volts (va)	0.27	
2. Ammeter at 250/2 amperes (va)	0.20	
Performance Data		
All errors are expressed as a percentage of the full-scale value.		
1. Full-scale self-heating error	< ±0.3	±0.6
2. Frequency error		
(*a*). 400–450 cycles	< ±0.3	< ±0.3
(*b*). 400–350 cycles	< ±0.3	+0.5
3. Ambient-temperature error		
(*a*). +25 C to +68 C	+0.9	−1.2
(*b*). +25 C to −38 C	−1.3	+1.8
(*c*). Permanent change	±0.3	±0.4
4. Error due to 10 instantaneous applications of 1,000 per cent load to ammeter	±0.6	
5. Effect of 95 per cent relative humidity at 65 C for 6 hours	±0.3	±0.5
6. Effect of vibration at 500–3,000 cycles per minute 0.020-inch amplitude	< ±0.3	< ±0.3



Figure 5. Ferromagnetic field structure of watt–varmeter with field-coil winding

pound. This weight advantage results not only from the measures just described but also from the method of support, consisting of a punched aluminum frame over which the winding is placed as shown in the illustration. A transformer of the same type and material for use on 60-cycle circuits would weigh about three or four pounds. The smallest usual 60-cycle current transformer of equivalent current rating weighs about 13 pounds.

Measurement of Active and Reactive Power

Since major loads are balanced and the system neutral is grounded, considerable saving in weight was achieved by the adoption of a single-phase element with the cross-phase connection for the measurement of vars as shown in Figure 4. In selecting the basic element, the following types were considered:

(*a*). Electrodynamic—iron core.
(*b*). Electrodynamic—air core.
(*c*). Induction-disk type.

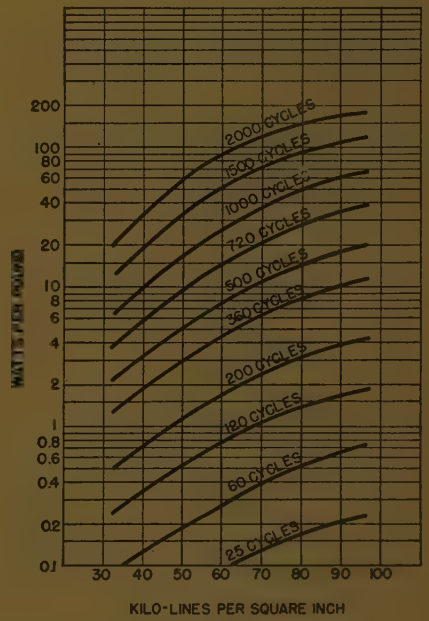


Figure 6. Curves showing typical core loss versus frequency for Nicaloi

Table II. Constants and Performance Data of 2 1/4-Inch Single-Phase Watt–Varmeter Rated 120 Volts, 1.11 Amperes, 133.3 Calibrating Watts With 0–50-Kw Scale for Three-Phase Four-Wire 208/120-Volt System and 125:1 Current-Transformer Ratio

Average of Seven Samples. Normal Accuracy—Two Per Cent of Full-Scale Value. Weight—0.58 Pound Each Including External Varmeter Resistor

Scale Constants	
1. Length (inches)	1.50
2. Angle of deflection (degrees)	90
Moving System	
1. Full-scale torque (T) (millimeter-grams)	0.35
2. Mechanical factor of merit (T/W)	1.06
3. Damping factor	2.0
4. Response time (seconds)	1.1
Potential Circuit	
1. Total terminal resistance (ohms)	12,000
2. Watts loss	1.20
3. Armature resistance (ohms)	46
4. Armature, reactance 400 cycles (ohms)	3.2
5. Maximum temperature rise of resistor spools (degrees centigrade)	37.5
Current Circuit (Field)	
1. Resistance (ohms)	0.10
2. Watts loss	0.12
3. Volt-ampere burden at rated current	2.5
4. Temperature rise (degrees centigrade)	18

Performance	Per Cent Full-Scale Value
1. Self-heating error (120 volts, 1.11 amperes, 400 cycles)	−0.3
2. Frequency error, unity power factor	
(*a*). 400–450 cycles	< −0.3
(*b*). 400–350 cycles	< −0.3
3. Ambient-temperature error	
(*a*). +25 C to +68 C	+1.1
(*b*). +25 C to −38 C	−2.1
(*c*). Permanent change	−0.5
4. Low-power-factor error at rated volt-amperes	
(*a*). 0.8 power factor	−0.5
(*b*). 0.5 power factor	−0.7
5. Varmeter operation	
(*a*). Zero power factor at instrument, rated volt-amperes	±0.9
6. Voltage error	
(*a*). 120–132 volts	+0.3
(*b*). 120–108 volts	−0.3
7. Sustained overload	
(*a*). Six hours at 100 per cent voltage, 120 per cent current	±0.3
8. Error due to 10 instantaneous applications of 1,000 per cent load to current circuit	< ±0.3
9. Effect of 95 per cent relative humidity at 65 C for 6 hours	+0.5
10. Effect of vibration for 2 hours at 500–3,000 cycles per minute at 0.020-inch amplitude	±1.0

The electrodynamic iron-core construction was chosen for the following reasons:

(*a*). For a given torque size and weight were minimum.

(*b*). The same mechanism can be used for other measurements, such as frequency.

(*c*). The scale is linear and consistently reproduceable, making possible a predetermined scale distribution.

(*d*). Inherent performance errors are small and can be compensated by known and simple means.

The magnetic structure of the wattmeter, Figure 5, comprises two stacks of laminations which overlap within the field coil and which are held in position by screws. The laminations are made of Nicaloi, 0.004 inch thick; a material which combines moderately high permeability with low core losses. The 400-cycle core losses at 50 kilolines per square inch, as shown by the curves of Figure 6, are more than 18 times the 60-cycle value. The magnetic material within the coil was operated at maximum density compatible with linear saturation characteristics and acceptable volt-ampere burden.

The electric circuit of the wattmeter, Figure 7, is composed of the field or current circuit and the armature or potential circuit. Each circuit is insulated from the other and from ground. The armature circuit includes two adjustable resistors and a reactance unit. These internal resistors provide an adjustment range of ±8 per cent of the instrument reading and are used for calibration adjustment during manufacture. The reactance compensates for the low-power-factor error originating from the phase-angle difference between field flux and field current due to core loss in the magnetic material. It introduces a corresponding phase-angle lag between potential-circuit voltage and armature current. The compensation is very good in the operating frequency range, and the maximum recorded instrument error at any load, zero power factor, did not exceed 1.5 per cent of the full-scale reading. The low-power-factor error is of importance when the instrument is used as a varmeter.

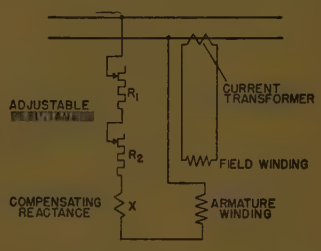


Figure 7. Electric circuit of 400-cycle watt–varmeter

Series reactance is used in the potential circuit to compensate for the phase angle between field flux and field current when measuring vars

Since the reactance of coils at 400 cycles is 6.7 times as great as at 60 cycles, it was necessary to use minimum ampere turns on the field to minimize the volt-ampere burden on the transformer. For this reason, the potential-circuit resistance was designed for 100 ohms per volt. At power frequencies, this resistance could be increased to 1,000 ohms per volt.

The constants and performance of the watt–varmeter, Table II, show reasonable conformity with the objectives of the design. The magnetic damping system, illustrated in Figure 8, consists of a small U-shaped Alnico II magnet and 0.008-inch aluminum disk. This system produces satisfactory dynamic characteristics which are independent of air density and altitude.

Measurement of Frequency

The design of the 120-volt 350–450-cycle frequency meter is based on the principle of series resonance at normal frequency. Of the several methods of application of this principle, the circuit which appeared to yield the minimum weight and size is that illustrated in Figure 9. This circuit consists of the field coil, further shown in Figure 10, and a moving coil connected through capacitor, reactor, and resistor to a secondary winding on the field coil.

The operation of the instrument is explained best by reference to the vector diagram. The field current, I_f, produces a field flux, ϕ_T, which induces the armature-circuit voltage, E_a. The flux ϕ in the air gap at the armature is a portion of ϕ_T. The reaction between ϕ and the armature current, I_a, produces the deflecting torque. At resonance, approximately 400 cycles, the armature current, I_a, is in quadrature with the field flux, and no torque is produced. At 350 cycles the armature current, I_a, leads the armature voltage, E_a, and produces a down-scale deflecting torque proportional to $I_a \sin \alpha$. At 450 cycles the armature current lags the voltage and produces an up-scale deflecting torque also proportional to $I_a \sin \alpha$. These deflecting torques vary with frequency, and true frequency indication is obtained by the addition of a restoring torque which is a function of the line voltage and armature displacement from the center scale position. This restoring torque is produced by a small *Mu*-metal vane[3] fixed to the



Figure 8. Small-panel 2 1/4-inch electrodynamic 400-cycle watt–varmeter with cover removed

The internal adjustable resistors are mounted on the insulating base

armature shaft which tends to align itself with the parallel field flux. This construction minimizes the influence of voltage on frequency indication because both the deflecting torque and restoring torque vary with the second power of the voltage.

The transformer shown in the circuit diagram, which consists of two windings on the same spool, is used to energize the armature circuit because the phase angle between E_a and ϕ will not change with increase in field resistance due to self-heating. Since no deflecting torque is produced at resonant frequency, changes in armature resistance do not appreciably affect accuracy at mid-scale frequencies.

The scale distribution, as analyzed in Appendix I, is open and readable throughout the frequency range, although not quite symmetrical with respect to the 400-cycle reference point.

Connections to the armature winding are made through low-torque spirals which are positioned to pull the pointer off scale to the left when the instrument is de-energized. This extraneous torque introduces a small voltage error and slightly changes the resonant-frequency adjustment.

In calibrating the frequency meter three adjustments are required:

1. The restoring torque is determined by the factory setting of the angular position of the armature vane.

2. The deflecting torque is adjusted to give the proper scale length by variation of the series rheostat, the effect of which is shown in Figure 11.

3. The pointer position is set by adjustment of the reactance.

To facilitate calibration, both coarse and fine adjustments are provided. The parallel lines, Figure 12, show that the pointer position can be controlled by this change in reactance without altering the scale length. These adjustments are used in the initial setting of the calibration at the factory.

Magnetic shielding of the frequency meter was necessary to eliminate stray fields which otherwise would produce a phase-angle change between the field flux in the transformer and that acting on the armature.

The characteristics and performance are tabulated in Table III. The minimum torque of the frequency meters is 33 per cent more than that of the wattmeters because of the heavier armature. The ratio of torque to armature weight varies from 0.5 to 1.0, depending on scale position. Ambient-temperature errors are due principally to changes in resistance of the armature circuits, the ohmic resistance of which is approximately 25 per cent copper. Changes in core loss with temperature introduce phase-angle errors. Both sources of error originate from the space limitations of the device. The performance is best in the operating range around 400 cycles.

Table III. Constants and Performance Data of $2^1/_4$-Inch 120-Volt 350–450-Cycle Self-Contained Frequency Meters

Average of Four Samples. Weight—0.83 Pound Each. Conditional Errors Are Expressed in Cycles

Scale Constants			
1. Length (inches)			1.53
2. Angle of deflection (degrees)			87.5
Moving System			
1. Equivalent* full-scale torque (T) (millimeter grams)			0.43
2. Equivalent mechanical factor of merit (T/W)			0.53
3. Response time# (seconds)			1.80
Volt-Ampere Burden at 120 Volts 400 Cycles			
1. Current (amperes)			0.113
2. Volt-amperes			13.6
3. Watts			1.9
Performance	**350 Cycles**	**400 Cycles**	**450 Cycles**
1. Self-heating error at 120 volts	+3.8	+2.8	+1.4
2. Voltage error			
(*a*). 120–132 volts	+3.3	+0.5	−4.6
(*b*). 120–108 volts	−3.5	−0.8	+3.6
3. Ambient-temperature error			
(*a*). +25 C to +68 C	±1.3	±0.9	±1.8
(*b*). +25 C to −38 C	−5.0	−3.9	−0.7
(*c*). Permanent change	±0.9	±1.0	±0.9
4. Effect of 95 per cent relative humidity and 65 C for 6 hours	< ±0.3	< ±0.3	±1.1
5. Effect of vibration for 2 hours at 500–3,000 cycles at 0.020-inch amplitude	+0.4	< ±0.3	+0.5

* This is the product of the torque gradient at 400 cycles and the scale angle.

\# The interval required for the pointer to indicate true frequency when instrument is energized at 400 cycles 120 volts.

Table IV. Weights of Typical Sample $2^1/_4$-Inch Aircraft-Power Panel Instruments

Instrument	Weight in Pounds D-C System	Weight in Pounds 400-Cycle A-C System
Ammeter (instrument only)	0.40	0.37
Voltmeter	0.41	0.37
Watt-varmeter		0.58
Frequency meter		0.83
Transformer		0.43
Shunt	1.80	
Total	2.61	2.58

Figure 9. Electric circuit and approximate vector diagram of 400-cycle self-contained $2^1/_4$-inch frequency meter

Operation depends on the unique variation with frequency of the phase angle between the armature current, I_a and the field flux, ϕ

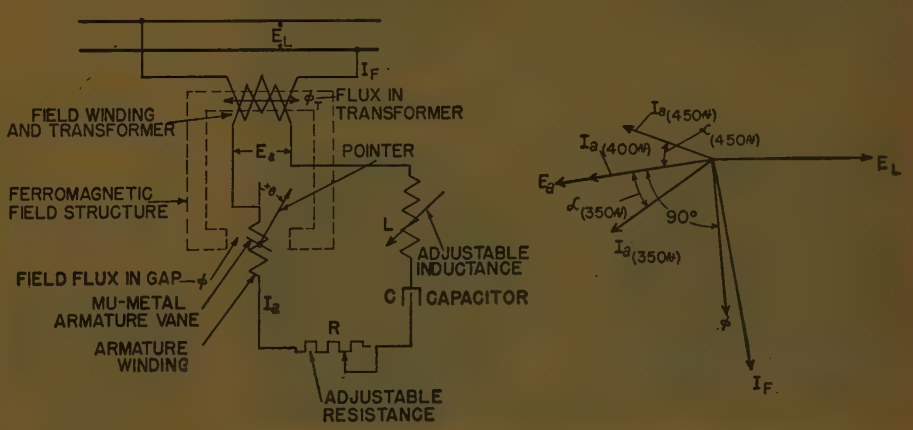




Figure 10. Ferromagnetic field structure of frequency meter with field-coil winding and internal transformer

Conclusion

The weight summary of Table IV compares the instrumentation of the 400-cycle a-c system with that of a 27-volt d-c system of equivalent capacity. The major component of the d-c system is the 1,200-ampere 50-millivolt shunt, while the weight of the a-c instrument group is more uniformly distributed with no single value exceeding one pound. The totals are substantially equal, and thus it is shown that the a-c instrumentation, including the measurement of watts, vars, and frequency, by careful design, can be accomplished without weight penalty. The function of instrumentation is the presentation of information in a manner suitable for quick and adequate perception. This factor has been fully recognized by designing instruments of uniform size, method of indication, and scale length, as shown in Figure 13. Uniform dynamic characteristics are obtained by magnetic damping of the moving systems. Performance data, while preliminary in char-

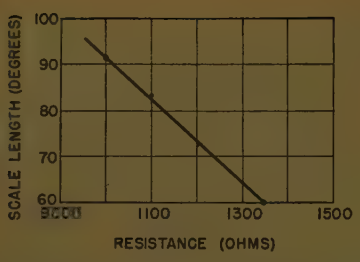


Figure 11. Curve showing scale arc length versus armature-circuit resistance for frequency meter

acter and including neither field nor flight tests, show no serious limitations in either accuracy or durability.

Appendix Calculation of the Scale Distribution of the Frequency Meter

The scale distribution of the frequency meter may be predetermined mathematically from the circuit constants and physical arrangement of the instrument parts shown in Figure 9. The three torques acting on the armature are evaluated and the equation of the scale distribution derived by equating their sum to zero. Numerical solution is obtained by applying the boundary conditions defined by the scale length and frequency range. Comparison of calculated with actual values is shown by the curves of Figure 14.

Assumptions

To simplify the solution the following assumptions are made:

1. The flux, ϕ, in the air gap is uniform, parallel, and normal to the pole faces.

2. The flux, ϕ, in the air gap is in phase with the flux, ϕ_T, in the field-coil transformer.

3. The flux, ϕ, in the air gap leads the armature voltage, E_a, by 90 electrical degrees.

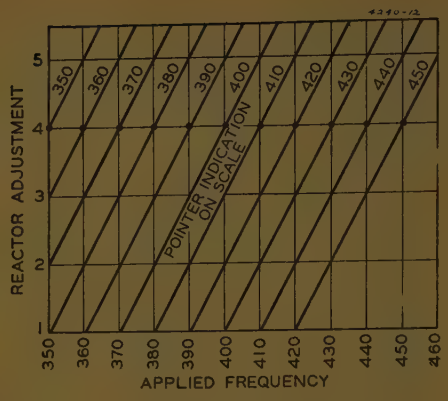


Figure 12. Curves showing range of adjustment for calibrating the frequency meter by changing armature-circuit inductance by means of the adjustable reactor

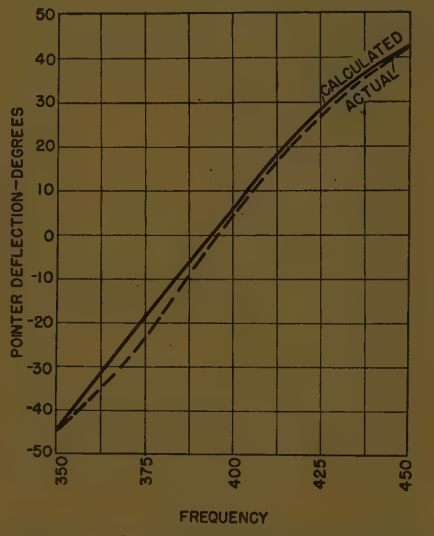


Figure 14. Curves showing actual scale distribution of one sample frequency meter and the theoretical distribution calculated from the known circuit constants and physical arrangement of the instrument parts



Figure 13. Small-panel 2 1/4-inch 400-cycle instruments for aircraft mounted on metal panel

4. The moving system is perfectly balanced, and friction is negligible.

Nomenclature

E_L = line voltage (volts)
E_A = armature-circuit voltage (volts)
I_F = field current (amperes)
ϕ_T = field flux induced by I_F (maxwells)
ϕ = field flux effective on armature in gap (maxwells)
N = number of turns on field coils
K = spring constant of spirals (millimeter grams per radian)
K_1, K_2, K_3, K_4, and so forth = constants
I_a = current in armature circuit (amperes)
Z_a = impedance of armature circuit (ohms)
X_L = inductive reactance of armature circuit (ohms)
X_C = capacitive reactance of armature circuit (ohms)
L = inductance of armature circuit (henrys)
C = capacitance in armature circuit (farads)
R_a = armature-circuit resistance (ohms)
S = initial deflection of spiral springs from center scale (radians)
θ = armature deflection (radians) (+ is clockwise). When $\theta = 0$,

(*a*). Pointer is at center scale.

(*b*). Spiral deflection is $+S$.

(*c*). Armature vane is in line with flux ϕ.

(*d*). Armature winding axis is at 90 degrees to flux ϕ.

y = moment arm of armature conductors
f = frequency, cycles per second
T_D = electrodynamic deflecting torque (millimeter grams) (+ is clockwise)
T_V = torque due to armature iron vane (millimeter grams) (+ is clockwise)
T_S = torque due to spirals (millimeter grams) (+ is clockwise)
α = phase angle between armature voltage and current
$\alpha = \tan^{-1}[(X_L - X_C)/R_a]$

Analysis

The electrodynamic deflecting torque, T_D, acting on the armature of the frequency instrument is

$$T_D = K_1 \phi y I_a \sin \alpha \qquad (1)$$

The flux, ϕ, in the air gap of the instrument due to the field current, I_F, which reacts with the armature current, I_a, may be expressed as

$$\phi = K_2 \frac{E_L}{fN} \qquad (2)$$

The component of the armature current, I_a, which is in phase with the field flux, ϕ, is

$$I_a \sin \alpha = \frac{E_a}{Z_a} \sin \alpha \qquad (3)$$

The moment arm of the armature conductor may be written

$$y = K_3 \cos \theta \qquad (4)$$

Equations 2, 3, and 4 may be substituted in equation 1:

$$T_D = K_4 \frac{E_L E_a}{fNZ_a} \sin \alpha \cos \theta \qquad (5)$$

A Distance Relay With Adjustable Phase-Angle Discrimination

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DISTANCE-TYPE transmission-line relays have been used very successfully for a number of years. Several different distance-type characteristics have been available. Among these characteristics are the impedance relay which measures impedance substantially independent of the phase angle, the reactance relay which responds only to the reactive component of the impedance, and a third characteristic which responds to a compromise between pure impedance and reactance. Another type of relay characteristic which combines impedance measurement with a directional indication has been used for a number of years as a fault-detector element to supervise another distance-measuring element.

These distance-type relays have been able to cope with most of the conditions encountered on the majority of transmission lines and systems. However, at present, the conditions which the relays must handle are becoming more and more difficult. This is because of increased loading especially on the longer transmission lines. Because of this it is becoming increasingly difficult for distance relays to distinguish between fault conditions and load conditions. It is the purpose of this paper to describe a new relay whose distance-response characteristic can be shifted or moded to fit different conditions. Distance elements which have capacity to discriminate between load currents and fault currents have been available but have been used mostly as fault-detector elements rather than distance-measuring elements to provide primary protection.

The relay to be described has directional discrimination as well as distance-measuring capacity and furthermore its directional discrimination can be regulated from zero to the maximum by the manipulation of suitable taps.

In applying relays to a transmission system it is necessary to state the relay characteristics in the same terms that the system conditions are stated. This is especially true of distance-type relays. If the relay characteristics are thought of in terms of volts and amperes, then the system conditions should be stated in the same terms. With the distance relays, however, it is difficult to think in terms of volts and amperes, because these values vary widely for the same relay response. This relay response is some function of the ratio between volts and amperes and, for any given value of the ratio, there may exist an infinite number of values of the volts and amperes. It therefore simplifies matters greatly to think of the distance-relay response in terms of the ratio of volts to amperes or in other words to think of the impedance, reactance, resistance, or combination thereof to which the relay responds. In applying these relays to a power system it is necessary to think of the system in the same terms. However, in designing distance relays it is necessary to think in terms of the volts, amperes, and phase angle to which it must respond because these are the quantities which actually operate the contact-actuating parts.

When distance relays are applied to transmission lines it is most convenient to plot both the relay characteristics and the transmission line conditions on an impedance diagram, that is, on rectangular co-ordinates of resistance and reactance. This gives a picture of the impedance values for which relay operation is desired, as well as a picture of the impedance values seen by the relay during load and synchronizing surge conditions, for which operation is not desired.

It is interesting to see what the various relay characteristics look like on a current-phase angle basis and what occurs when transfer is made to the R and jX diagram. When the current response against a given voltage is plotted against phase angles, the loci of all distance-type relays whether impedance, reactance, resistance, or any combination thereof are circles if we consider the straight line as the special case of a circle with infinite radius. No attempt here is made to provide rigorous proof of this statement as this has been covered in various publications. It is instructive to examine the various distance-relay characteristics on the current-angle diagram and then transfer them to the resistance and reactance co-ordinates.

With reference to Figure 1, the impedance relay has the response circle shown with its center at the origin. It is obvious that the current locus must be a circle since a pure-impedance relay will operate against a given voltage restraint at the same current value regardless of the phase angle between the current and voltage. To transfer this circular characteristic to the resistance-reactance co-ordinates in Figure 2, it is necessary only to divide the constant-voltage restraint by the current pickup values at different angles and plot the resultant impedance

---

The restoring torque acting on the armature due to its iron vane tending to align itself with the field flux, ϕ, is

$$T_V = -K_5\left(\frac{E_L}{fN}\right)^2 \sin 2\theta \qquad (6)$$

The extraneous torque due to the lead-in spiral springs is

$$T_S = -K(\theta+S) \qquad (7)$$

Since the summation of torques acting on the armature must be zero, for steady-state indication,

$$T_D+T_V+T_S=0 \qquad (8)$$

If equations 5, 6, and 7 are substituted in equation 8:

$$K_4\frac{E_L E_a}{fNZ_a}\sin\alpha\cos\theta - K_5\left(\frac{E_L}{fN}\right)^2\sin 2\theta - K(\theta+S)=0 \qquad (9)$$

This is the equation for the scale distribution. For the purpose of determining the values of K_4 and K_5, the following boundary conditions are assumed which correspond to the scale length of one of the sample instruments:

1. At $f=350$ cycles the pointer deflection is $\theta=-44$ degrees.
2. At $f=450$ cycles the pointer deflection is $\theta=+43$ degrees.

By substituting these values and the known constants, equation 9 is reduced to the following functions of frequency and deflection:

$$(2.4)10^6\frac{\sin\alpha\cos\theta}{fZ_a} - (1.56)10^4\frac{\sin 2\theta}{f^2} - 0.038(\theta+1)=0 \qquad (10)$$

Equation 10 defines the theoretical scale distribution in terms of pointer deflection, θ; frequency, f; and known constants. The solution to equation 10 is shown by the calculated curve of Figure 14 and is compared to the distribution of one of the sample instruments by the "actual" curve. Deviation between the curves is due mostly to the nonuniform distribution of flux in the air gap.

References

1. WHY TOMORROW'S PLANES WILL HAVE A-C ELECTRICAL SYSTEMS, T. B. Holliday. *Electrical Manufacturing*, July 1943.

2. AMERICAN STANDARD DEFINITIONS OF ELECTRICAL TERMS (book), C42. AIEE, 1941. Page 109.

3. AN ELECTRODYNAMIC MOVING-IRON MECHANISM FOR ELECTRICAL MEASURING INSTRUMENTS, T. A. Rich. *General Electric Review*, volume 38, July 1935, pages 334–8.

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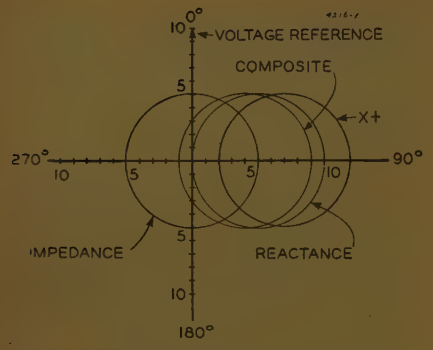


Figure 1. Current–phase-angle diagram of impedance, reactance, and composite characteristics

values at the corresponding angles on the R–X diagram. Obviously the current circle of Figure 1 with its center at the origin is also an impedance circle in Figure 2 with its center at the origin. If the current circle, Figure 1, were to be moved to the right along the 90–270-degree axis until it is tangent to the 0–180-degree axis it then becomes a pure reactance characteristic. Plotting the reciprocal of the current-pickup values given by this circle at different angles on the R–X diagram at corresponding angles gives the straight-line reactance characteristic in Figure 2. Placing the circle at a position between the impedance and reactance circles in Figure 1 gives a composite characteristic depicted by the large circle in Figure 2. Pushing the circle in Figure 1 beyond the reactance circle such as at $X+$ bends the reactance characteristic in Figure 2 upward into the circle shown.

While in the foregoing discussion the characteristic circle was shifted along the X axis, it is to be understood that this shifting process can be performed along any angular axis.

In Figure 1 the current-locus circle

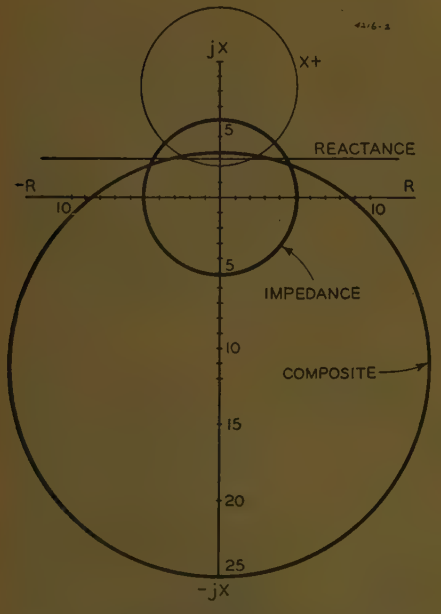


Figure 2. Resistance–reactance diagram of current loci of Figure 1

was shifted to the right by bodily moving the circle over. The circle can be shifted in another manner, that is, by holding a point on the circle stationary and then expanding its diameter to minus infinity after which its diameter is reduced from positive infinity. Figure 3 shows this transformation where the circles are all passed through the point $I=5$. Transferring these circles to the R–X diagram yields the interesting set of circles in Figure 4. Note that the impedance circle decreases in diameter and moves upward. The movement of the center of the circle away from the origin means that the impedance characteristic acquires a sense of direction. In other words, it means that response to impedance values in some directions is greater than its response in other directions. When the circle becomes tangent to the R axis, it

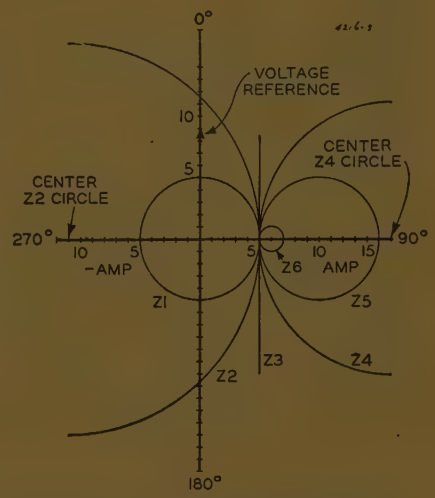


Figure 3. Current–phase-angle diagram of various impedance characteristics

means that the impedance characteristic has become completely directional.

In applying distance relays to transmission lines it is convenient to plot the conditions for which tripping is desired as well as the nontripping conditions on resistance and reactance co-ordinates. It is easy to plot the fault ohms on this diagram as the resistance and reactance ohms of the transmission line are readily available. Thus in Figure 5 the line L_F–L_F' represents the line on which the impedance ohms of the line fall, and the point B represents the ohms for a solid fault at the far bus. The vector L_{FR} represents the apparent impedance value when fault arc resistance is included. On short lines the line ohm vector is the only one to be considered. However, on the longer heavily loaded lines it is usually necessary to consider the impedance values seen by the relays during normal load conditions and during surge conditions when synchronizing current is flowing over the line to keep the generating sources in step. While it is easy to plot the normal-load-impedance vector it is generally much more difficult to plot the values representing momentary load swings and synchronizing surges. The exact location of the impedance vectors for the latter conditions involves considerable calculation but methods for doing this have been covered previously. An approximate idea of the manner in which the synchronizing-surge-impedance vector shifts can be gained by locating the point along the line L_F which represents the number of ohms the electrical center is away from the relay location. The load or surge-impedance vector will terminate in a straight line which intersects the line L_F at the electrical center. The more severe the swing condition the closer this vector will approach the line L_F. Some particular maximum approach to the line L_F will represent the dividing line between ability of the system to stay in step or to develop into a definite out-of-step condition. Generally it is desirable to permit tripping for all surge conditions from which the system cannot recover but to prevent tripping for all load conditions and surges up to this nonrecovering point. The object in applying distance relays to transmission lines is to utilize a relay characteristic that will trip definitely for faults on the line but not trip for swing conditions from which the system can recover. This means that the relay-response characteristic, when plotted on the R–X diagram, must encircle properly the line ohms including fault resistance but must not encircle the swing ohms. Realizing that the minimum synchronizing surge ohms for which it is not desired to trip may fall quite close to the fault-ohm line it becomes evident that a characteristic similar to Figure 5 is necessary. Such a characteristic could enclose the line ohms but exclude the synchronizing-surge ohms if the diameter of the circle, the position of the circle on an axis, and the angular position of the axis were all adjustable.

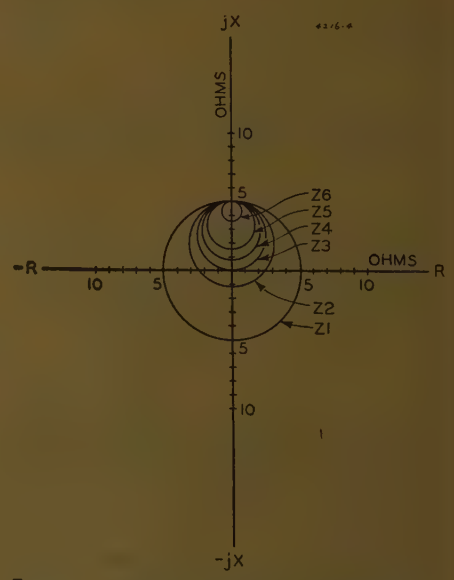


Figure 4. Circles of Figure 3 plotted on R and X co-ordinates

Such a characteristic has been imparted to the beam-type impedance relay by making the restraint proportional not to voltage alone but proportional to the vector sum of the fault voltage and the fault current while the operating force is made proportional to the fault current. Figure 6 shows the schematic connections. The delta current I_A-I_B is applied to the primary winding of a current transformer and to the operating coil on the beam element. The secondary of the current transformer being shunted by the reactor X_c causes a voltage drop across the resistor R_t which leads the current by 60 degrees. A portion of the voltage R_t is introduced into the circuit supplying voltage to the standard split-phase potential-restraint coils of the beam element. The voltage E_{AB} is fed through the phase shifter as shown. As a result of this arrangement the energy fed into the potential-restraint coils of the impedance element is proportional to the vector sum of E_{AB} and I_A-I_B.

The action which takes place to cause the element to have a circular characteristic can be seen from the vector diagram in Figure 7. This is drawn for the case where the center of the circle is to be placed on the X axis and the circle is to pass through zero. Assume that the fault current $I_A-I_B=I_R$ lags the fault voltage E_{AB} by 90 degrees and that under this condition the current in the potential-restraint coils due to the fault current I_R is 180 degrees out of phase with the current due to the restraint voltage E_{AB}.

The operating energy on the front end of the beam is represented by the vector I_0, although this is the same current as that operating on the rear end of the beam, while the current on the front end is shown as a vector, no vector relationship exists between the pull on the front end and the pull on the back end of the beam, since the beam has substantially no response to the phase angle between the forces on the two ends. It is arranged that the current I_0 produces a mechanical force on the front end of the beam equal to the force produced by the current on the back end of the beam when no voltage is present. For fault currents lagging 90 degrees it is easy to establish the conditions for a balance point. For instance, if the voltage E is assumed to be 12 units, then it is evident that the current I_R directly opposite must be 6 units in order for the net restraint of 6 units just to balance an operating force of 6 units. If the current lags the voltage less than 90 degrees (say I_R'), it must be increased to a value such that the vector sum of I_R and E_1 (or I_s) still equals I_R because the operating current I_0 has increased at the same rate, in order to maintain a balance point. It can be seen from an inspection of Figure 7 that the current I_R and I_0 for any angle must terminate on the straight line MM in order to make the scalar value of the vector sum $E_1+I_R=I_s=I_R=I_0$. In other words, the line MM represents the locus of balance currents for a given voltage E_1. Division of the voltage E_1 by the balance currents gives impedance values, which plotted on R and X co-ordinates gives a circle with its center on the X axis and passing through zero. The pull of the current on the restraint end of the beam must be equal to the pull of the current on the other end in order for the current locus to become a straight line. If the current pulls do not balance the characteristic becomes a circle above or below the straight line depending upon which way the beam is unbalanced so that on current alone one unit of I_0 is required to balance two units of I_R (or $I_0=2I_R$), and the characteristic becomes a circle as shown. Under this condition one balance point with voltage applied will be when $12-I_R=I_0=2I_R$ or $I_R=4$ units. The other balance point is given by $I_R-12=I_0=2I_R$ or $I_R=-12$. On the R and X diagram this is a circle with its center on the X axis and the origin is within the circle. If $I_0=I_R/2$, that is, the current on the restraint end of the beam is predominant, then we obtain the circle shown which on the R and X diagram does not contain the origin.

It is clear from the foregoing that the introduction of current into the voltage-restraint circuit has performed the circle inversion depicted in Figures 3 and 4. However, in these figures the current circle was inverted by holding a point on its periphery fixed. This caused the impedance circle, Figure 4, to diminish in diameter as it was shifted. It is, of course, desirable that the impedance-circle diameter remain constant while the center is shifted. The arrangement shown in Figure 6 accomplishes this purpose. The location of the circle on the R and X diagram is dependent upon the ratio of I_0 to I_R while the radius is dependent only upon I_0. Figure 8 has been drawn for different ratios of I_0 to I_R and is similar to Figure 3 except that the circles do not go through a common point. Figure 9 shows these coils transferred to the R and X diagram. The displacement of the R and X circle with constant diameter to infinity corresponds to the disappearance of the current circle at the origin when I_R is made infinitely large compared to I_0.

The circuit shown schematically in Figure 6 provides independent adjustment of the diameter of the impedance circle, its location on an axis, and the angle of the axis. The following considerations will bring out the method of accomplishing these adjustments. Consider for the moment that the reactor X_c and the secondary of the reactor X_s are open-

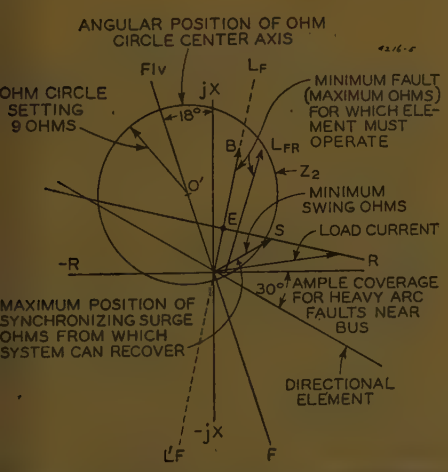


Figure 5. Line and relay characteristics plotted on R and X co-ordinates

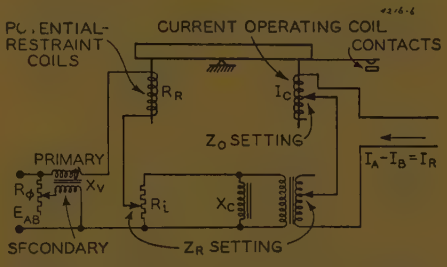


Figure 6. Schematic connections of impedance relay with adjustable characteristics

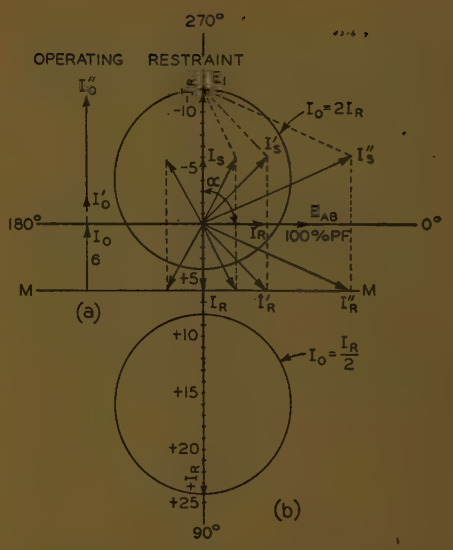


Figure 7. Vector diagram of current and voltage forces acting on the beam

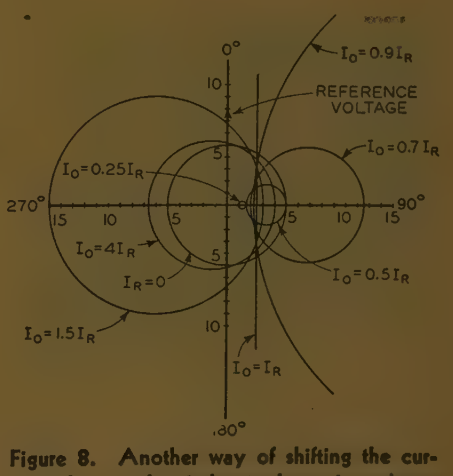


Figure 8. Another way of shifting the current-phase-angle circles to keep impedance circle radius constant

circuited. The current in the potential-restraint coils R_R contributed by the voltage drop R_t across the resistor fed by the current I_A–I_B is determined by the impedance of the reactor primary Z_v, the potential coil R_R, and the resistance R_t. The voltage source E_{AB} is considered to be effectively short-circuited. The current flowing in the restraint circuit because of the current I_A–I_B alone will, therefore, lag behind the voltage $(I_A–I_B)R$ by an angle B. Now consider the current in the restraint coils because of the voltage E_{AB} alone. The parallel impedance of the current-transformer secondary can be considered infinitely large. The current in the relay due to E_{AB} then is determined by the impedances Z_v, R_R, and R_t and it will lag the voltage E_{AB} by the same angle B. Therefore, the current in the restraint coils due to the voltage E_{AB} and the current I_A–I_B will be in phase or 180 degrees out of phase when I_A–I_B is in phase with E_{AB}. Now consider what is necessary to place the characteristic circle on the R and X diagram so that its center is on an axis 60 degrees lagging the R axis and so that the circle passes through the origin. From Figure 7 it is seen that an R and X circle passing through the origin requires a straight-line current locus as shown. Also note that the straight-line locus always occurs perpendicular to the current in the restraint coils contributed by the fault current I_A–I_B when this current is directly opposite to the current in the restraint coils produced by the voltage E_{AB}. These two currents are directly opposite when I_A–I_B is in phase with E_{AB} as was just shown. Therefore, if the voltage across the resistor R_t is caused to lead I_A–I_B by 60 degrees the direct opposition of voltage and current ampere turns in the restraint coils will occur when the fault current I_A–I_B lags E_{AB} by 60 degrees thus placing the R and X circle on the 60-degree axis. The circle, of course, is made to pass through

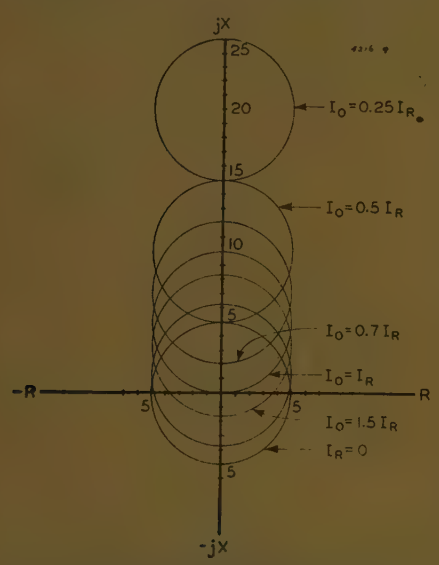


Figure 9. The impedance circles on R and X co-ordinates resulting from Figure 8

the origin by making the pull of the current I_0 on the operating side of the beam just balance that of the current I_R on the restraint side. This is done merely by regulating the number of turns and core-screw setting on the impedance element and the turns on the transformer primary and the tap on the resistance R_t. For complete flexibility it is desirable to be able to shift the axis along which the characteristic is to be moved to a maximum of 120 degrees. This means that when the fault current I_A–I_B lags the fault voltage E_{AB} by 120 degrees the current and voltage energizations in the restraint coils must be in direct opposition. Since the current in the resistor R_t was advanced in phase a fixed amount of 60 degrees by adding the shunt reactor the only way to obtain 120 degrees is to lag the current in the restraint coils due to the voltage E_{AB} by 60 degrees. This is done by passing a current through the secondary winding of the reactor X_v in phase with E_{AB} because of the resistor R. This adds a component of current almost at 90 degrees to the current already in the primary and, therefore, the phase angle of the current with respect to E_{AB} can be varied over a range of 60 degrees by regulating the amount of the resistance R.

By using a circuit similar to Figure 6 to provide the vector sum of the current and voltage to the contact end of the beam with a restraint proportional to voltage it is possible, by the proper selection of constants and phase shifts, to produce the type of characteristic depicted in Figures 1 and 2. This was covered in a paper before the Institute several years ago.

Course adjustment of the center of the circle is provided by the primary taps on the current transformer while continuous adjustment between taps is provided by a slider on the resistor R_t. The diameter of the circle is controlled by the standard tap and core-screw (TS) adjustment on the impedance-element current coil. The axis of the circle is rotated by adjustment of the resistor R_ϕ. All of these adjustments are entirely independent and anyone can be changed without affecting the others.

It becomes evident that a relay with such a flexible characteristic materially aids in the problem of avoiding tripping on load currents and synchronizing currents without jeopardizing the ability to trip faults. With reference to Figure 5, the circle $Z2$ is an example showing how the characteristic can be placed so that the fault ohms L_{FR} can be included in the circle and still have the much shorter synchronizing-surge vector fall outside the tripping zone.

Figure 10 shows the range over which the new relay characteristic can be adjusted. The current-transformer primary and resistor setting R_t are calibrated in ohms (Z_R) and when Z_R=0, the center of the characteristic circle is at the origin or in other words the characteristic is pure impedance without directional dis-

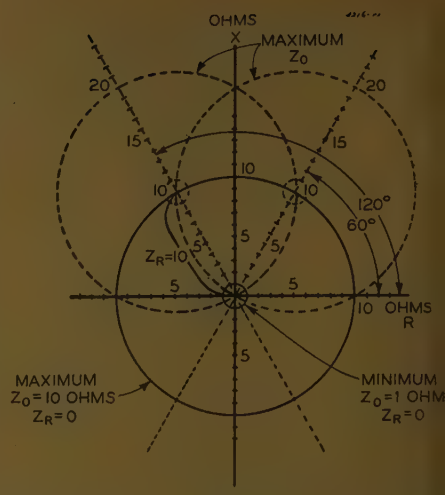


Figure 10. Range of adjustment of impedance characteristic of new relay

crimination. The radius of the circle is determined by the tap and core-screw setting on the beam-current coil. For any value of the radius the center of the circle can be shifted up the axis by the setting Z_R without changing the radius of the circle. The circle can be shifted in the opposite direction merely by reversing the polarity of the current or voltage. In addition to this the axis on which the circles are shifted can be rotated between 60 degrees and 120 degrees lagging in steps of 12 degrees by adjusting R_ϕ.

The adjusting network is housed in an external case there being three networks in each case so that each distance element of the standard three-zone impedance relay can be adjusted. Furthermore, the relay (type HZM) and the external adjusting case are arranged so that the characteristic of either one, two, or three distance elements of the relay can be supplied with the modified characteristic.

Conclusions

1. The utilization of the vector sum of the current and voltage on the operating or restraint side of a beam-type distance relay makes it possible to impart any type of circular characteristic whatsoever to the distance element.

2. The ability to introduce an adjustable amount of phase-angle discrimination into an impedance characteristic means that, once the transmission-line ohm conditions are known, the relay characteristic can be readily molded to fit the conditions best.

References

1. Performance Requirements for Relays on Unusually Long Transmission Lines, F. C. Poage, C. A. Streifus, D. M. MacGregor, E. E. George. AIEE Transactions, volume 62, 1943, June section, pages 275–83.

2. Protective Relaying for Long Transmission Lines, A. R. Van C. Warrington. AIEE Transactions, volume 62, 1943, June section, pages 261–8.

3. Transmission and Relaying Problems on the Fort Peck Project, Erik Floor, H. N. Muller, S. L. Goldsborough. AIEE Transactions, volume 63, 1944, May section, pages 209–14.

4. A New High-Speed Distance Relay, S. L. Goldsborough, W. A. Lewis. Electrical Engineering, volume 51, March 1932, pages 157–60.

Carbon-Pile Voltage Regulators for Aircraft

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NONMEMBER AIEE

WITH the increase in size of modern military aircraft and the complexities of its equipment, it has been necessary to enlarge greatly its electric system. Up to the time of the beginning of the second World War in Europe, the Tirrell and step-type regulators were used universally to regulate the voltage of aircraft generators, alternators, and motor generator sets. With the coming of lightweight 30-volt 100- and 200-ampere generators, the field current required began to exceed the limitations of the Tirrell regulator. It became apparent that a high-field-current voltage regulator would have to be developed since the appreciable weight saving gained by designing generators with high field currents is so vital to modern aircraft.

This requirement was met by adapting the well-known art of carbon-pile regulation, long-used in railroad lighting equipment, to aircraft. It is the purpose of this paper not to dwell too deeply on the actual design features of the regulator itself, but to acquaint the reader with a general knowledge of its principle of operation, and to discuss its advantages and disadvantages and its numerous applications.

General Construction

Figure 1* shows a sectional view of a conventional present-day carbon-pile regulator, and Figure 2 shows the same regulator not sectionalized. Close examination of Figure 1 discloses that the regulator consists primarily of

(*a*). An ironclad magnetic circuit enclosing a controlling solenoid and an adjustable core.

(*b*). An armature assembly which is mounted on radial leaf springs with a center contact plug insulated from the armature assembly which is the electric connection to the movable end of the carbon pile.

(*c*). The carbon pile consisting of precision-made carbon disks surrounded by and held in place by an insulating tube which in turn is enclosed by a finned housing for dissipating the heat from the carbon pile.

(*d*). An adjustable screw insulated from the rest of the regulator and containing a carbon button. This screw and button acts as the contact for the stationary end of the carbon pile.

Other details of Figure 1 show the regulator proper mounted on a quickly detachable mounting base on which is mounted a fixed resistor and a variable rheostat for raising or lowering the regulated voltage.

Principle of Operation

When the solenoid is unenergized, the armature is pushed against the carbon stack by the springs, thus compressing the carbon pile so that its resistance is at its very minimum. Energizing the solenoid causes the armature to be magnetically attracted to the ironclad magnet case. Magnetic force acts in the opposite direction to the spring force. The compression force exerted on the carbon pile is the difference between the spring force and the magnetic force. It is this varying magnetic force, created by the solenoid, that tends to overcome the spring force and thus vary the pressure on the carbon pile, and hence its resistance. Any change in the resistance of the carbon pile directly effects the output of the machine whose voltage is being regulated, which in turn tends to raise or lower the voltage across the solenoid. Since the regulator is extremely sensitive to any change of *NI* in the solenoid, a balance of magnetic and physical forces takes place in the regulator, and regulation becomes automatic.

Temperature compensation is obtained by the proper selection of the resistance ratio in the coil circuit and by matching a bimetallic spring support to the expansion characteristic of the regulator. Total resistance of the coil circuit consists of copper resistance and an external near-zero-temperature-coefficient resistance connected in series. The bimetal has a slight angle on which the armature springs rest. With an increase in temperature, the bimetallic washer tends to flatten out or the angle decreases. In this way the pressure exerted by the springs lessens. This in turn compensates for the decrease in the magnetic pull resulting from the heating of the copper solenoid coil and other parts affected by temperature. Present-day regulators are temperature-compensated to give satisfactory voltage regulation at ambient temperatures ranging from −50 to +71 degrees centigrade.

Adjustment of the Regulator

Three separate adjustments are provided in the regulator:

1. Coil-circuit resistance or coil-current.
2. Core or air-gap.
3. Carbon-pile compression-screw or spring-tension adjustment.

Adjustment of Coil-Circuit Resistance

Coil-circuit resistance is set by means of the adjustable resistor or rheostat to a value predetermined by the manufacturer. In cases where adjustable rheostats are used, the resistances are so chosen that the contact slider arm will be set in the middle of the rheostat resistance range. This will permit a raising or lowering of the regulated voltage after the regulators have been adjusted properly and put into service. Once the coil-circuit resistance has been set, it should not be changed throughout the remainder of the adjustment procedure since the proper *NI* in the solenoid in this way has been fixed for a given regulated voltage. Also, a proper resistance ratio is established to give good temperature compensation.

Preadjustment of the Core

When the regulator is first assembled at the factory, the core is screwed in until its face is just flush with the face of the magnet case next to the armature assembly. At this point the core is center-punch-marked at the adjustment end, and a corresponding center-punch mark is put on the end plate. In this way, it is possible to line up the two cen-

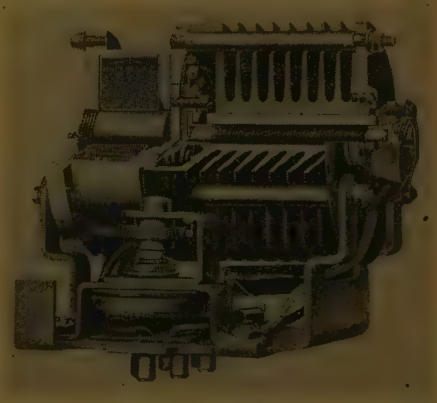


Figure 1. Sectional view of modern carbon-pile regulator

This unit regulates d-c generator output for both single- and parallel-generator systems

* The regulator, as shown in Figures 1, 2, and 4, is of the type disclosed in Newton United States patent 2,268,718 under which the United States Government holds a nonexclusive license for the duration of the war.

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W. G. Neild is with the Eclipse-Pioneer division of Bendix Aviation Corporation, Teterboro, N. J.

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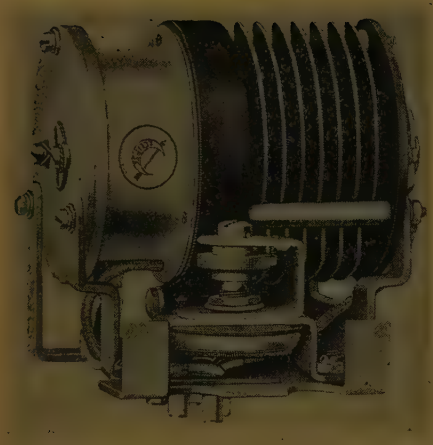


Figure 2. Same unit as shown in Figure 1, except not sectionalized

This carbon-pile voltage regulator is for use in conjunction with single-voltage generators rated from 1.5 to 12 kw

ter-punch marks after the entire regulator has been assembled and thus set the core flush with the magnet case. Correct set position of the core should be such that the core air gap is slightly greater than when set in the flush position.

Adjustment of Pile Compression Screws

With the regulator adjusted as already indicated, the carbon pile should be connected in series with the control circuit of the unit whose voltage is to be regulated and the coil circuit connected across the output. Run the unit so that it would normally produce its rated output if the regulator were properly set. For the purpose of illustration, assume that the carbon-pile compression screw is just turned in to the point at which the contact plug makes contact with the carbon pile. As the pile compression screw is turned in, the entire armature assembly will be moved toward magnet case, increasing the spring tension and decreasing the air gap between the armature and core. Increasing the spring tension will cause the carbon pile to be correspondingly compressed, and its resistance will decrease. Continued inward motion of the pile compression screw will cause the regulated voltage to rise to a maximum point *B*, (Figure 3), decrease to a minimum point *D*, and then increase very rapidly from *D* to *E*. From *A* to *B* and from *E* to *F* (Figure 3) the unit fails to function as a regulator, but acts as a manual carbon-pile rheostat. In the region of the curve from *B* to *E* the unit acts as an automatic carbon-pile rheostat or regulator. However, between *B* and *C* on the curve, the regulator will pass through an instability or hunting stage, becoming less and less violent and finally disappearing as point *C* is approached. It is obvious that no instability in the regulator can be tolerated at any time, as this causes the carbon pile actually to open the circuit, and a destructive arc results which burns the carbon disks, greatly shortening their life and, hence, the useful life of the regulator. Instability in the carbon pile can be detected readily by connecting a sensitive undampened voltmeter, an oscillograph or a set of earphones with at least 1,500 ohms impedance, across the carbon pile, the oscillograph and earphones being the most sensitive. Fluctuation of the carbon-pile voltage will cause the voltmeter needle to fluctuate and will cause the earphones to become very noisy. In the case of the earphones, violent instability will cause an unbearable noise as compared to a quiet hum when stable operation of the regulator is obtained. Best regulating position of the pile screw occurs between *C* and *E*. In general, a setting of the pile compression screw to the left of the minimum point *D* will give a flat or rising voltage characteristic with load as the point set nears the instability region. Likewise, a setting to the right of the minimum point *D* will give a drooping voltage characteristic with load and will be a more stable setting of the regulator. When the approximate correct setting is obtained by the pile compression screw, the regulator voltage probably will be too low. If this is the case, the regulated voltage can be raised by turning the core plug out very slowly, thus increasing the air gap. Regulated voltage is lowered by turning the core plug in, but under no circumstances should the face of the core plug be turned in beyond the flush position. After the core plug is adjusted, the compression-screw adjustment should be rechecked. This procedure should be repeated as many times as necessary until no further adjustment of either the core plug or carbon-pile compression screw is required. All locking screws should be tight. Final setting must be such that the regulator is stable under all possible operating conditions of speed, load, or input voltage, or the useful life of the carbon pile will be short. Much can be written on the regulator adjustment in specific cases, but the aforementioned procedure, in general, applies to all carbon-pile regulators. In actual cases where units are set at the factory, the violent instability region of the curve is smoothed out by lowering the input voltage or by reducing the speed, in the case of generators, to values far below that at which they normally operate. In this way it is possible to adjust the carbon-pile compression screw and follow through the curve to point *C* or *D* without any instability whatever. After the pile compression screw has been adjusted to this point, the speed or voltage may be raised and finer adjustments made without damage to the carbon pile caused by arcing between the carbon disks. All adjustments should be made only by trained personnel and with the proper equipment, as wrong adjustments result in high voltage which is disastrous to most loads.

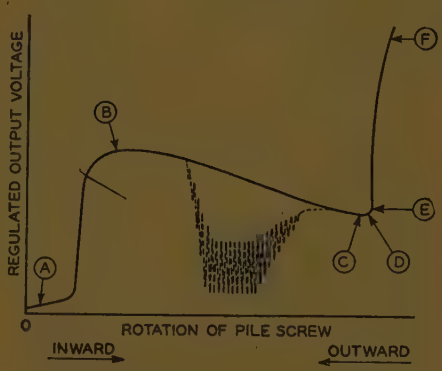


Figure 3. Effect of pile-screw rotation on regulated output voltage

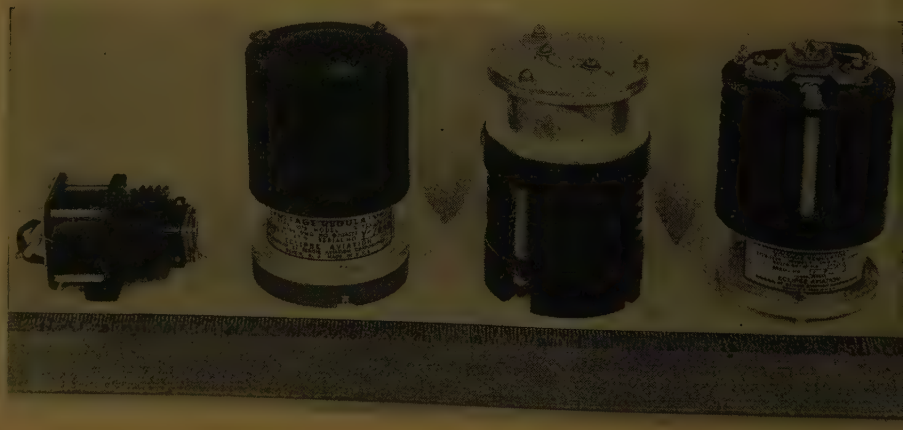
If regulator becomes unstable, voltmeter needle will fluctuate rapidly, and that portion of voltage characteristic will be as shown by dotted lines in diagram. Best regulating position of pile screw is shown at *C*

Figure 4. Four different-wattage-size regulators

Left to right, 20, 35, 50, and 75 watts continuous dissipation in the carbon pile. Ratings of all can be safely raised by forced ventilation

Practical Application

At the present time, there are four different-sized regulators in production based upon the maximum continuous



wattage dissipation in the carbon pile; namely, 20, 35, 50, and 75 watts (see Figure 4).

All of the preceding can have their ratings increased greatly by forced ventilation. Although all four sizes use the same-diameter carbon disks, the lengths of the carbon piles are different. The smallest or 20-watt regulator weighs $9^1/_4$ ounces and uses a one-half-inch stack of carbon disks; the 35-watt size weighs two pounds, three ounces, and uses $1^1/_2$-inch carbon pile; the 50-watt size weighs $2^1/_2$ pounds (with base shown) and has $1^1/_2$-inch carbon pile; and the 75-watt size weighs two pounds, three ounces, and has a two-inch carbon pile. Carbon piles are available in various different carbon-disk thicknesses and grades. By varying the length, grade, and disk thickness, many different resistance ranges can be obtained. Any of the aforementioned regulators can be made to regulate different given voltages by the selection of the proper solenoid coil.

Carbon-pile regulators have been used in many different applications. For the most part, these applications can be divided into three different classifications, namely:

(*a*). Those regulating the output of engine-driven d-c generators.

(*b*). Those regulating the output of high- and low-frequency engine-driven alternators and motor-alternator sets.

(*c*). Line voltage regulators.

When used to regulate the output of engine-driven d-c generators, the generator is usually a shunt type for use on 12- or 24-volt systems, although there are cases where 115 volts d-c systems are employed as well. In all of the preceding systems, the carbon pile is connected in series with the shunt field of the generator, and the solenoid coil circuit is connected across the output of the generator.

Parallel Operation

Where generators are operated in parallel as in multiengine airplanes, each generator is made to deliver its share of the load by the use of a load-compensating coil wound as a separate winding on top of the main regulating coil in the regulator. This compensating winding is used in conjunction with a calibrated drop connected between generator negative and ground. In some cases the drop through the series winding is utilized. With the aid of an equalizer bus, if one generator carries more than its share of the load, the voltage drop will be greater in its circuit than the others.

A current will flow through its equalizer coil which will lower its voltage and hence its load and, at the same time, boost the voltage and the load in the other generators. In that way it equalizes the loads. Very little change in voltage is necessary to make the generators parallel properly, and practically no change in the bus voltage will be noted.

Other Applications

Other applications of carbon-pile regulators in conjunction with generators consist of current limiters and wattage limiters. Current limiters limit the output current of the generator to a predetermined value, usually full load plus ten per cent. Its carbon pile is connected in series with voltage-regulator carbon pile, and its coil consists of a few turns of heavy conductor connected in series with the line. When the current reaches the predetermined value, the carbon pile begins to increase its resistance, decreasing terminal voltage and limiting the output current. For the most part current limiters are not used, because modern aircraft generators are designed to carry heavy overloads without damage for the short duration of landings or take-offs.

Wattage limiters are much the same as current limiters and work the same way, except that they have an extra potential winding on the same coil as the current winding and are used where generators are driven by auxiliary gasoline engines. The purpose of this type of regulator is to limit output of generator to safe value so that excessive overloads will not stall

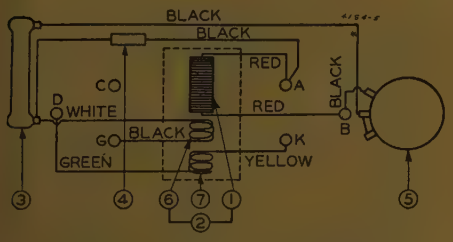


Figure 5. Internal wiring diagram

75-watt carbon-pile regulator with detachable base shown in Figures 1 and 2

1—Carbon pile
2—Regulator assembly
3—Resistor—tubular
4—Resistor—1,500 ohms
5—Rheostat
6—Coil—shunt
7—Coil—equalizer

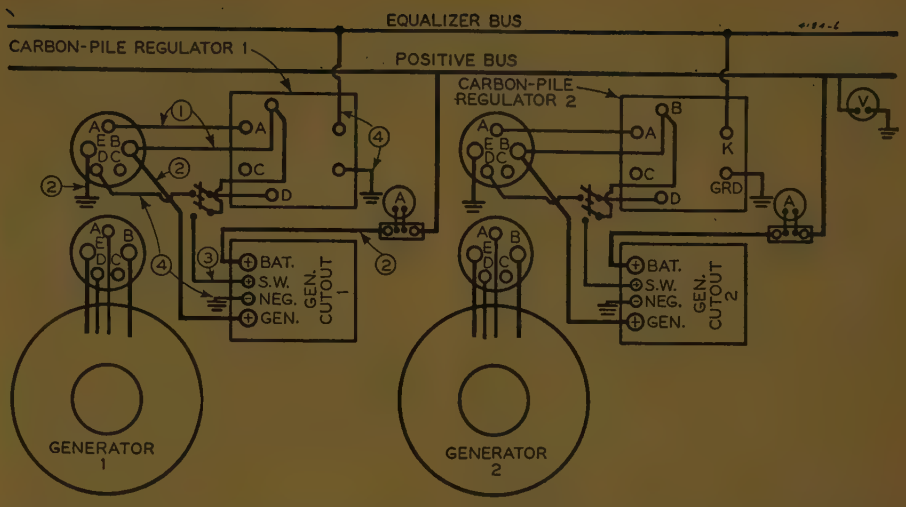


Figure 6. Typical schematic wiring diagram showing installation of *M-2*, *O-4*, and *P-2* generators with carbon-pile voltage regulator

For parallel generating systems, any number of generators may be connected in the manner depicted in these diagrams, whereas for single systems generator 2 and its associated equipment and connections are omitted from the systems shown. Also to be omitted are the equalizer bus and connections to the *D* and *K* carbon-pile regulator terminals, with use of a single-pole single-throw line switch Caution: Ground (*E*, receptacle) connection of each generator in parallel system shown must provide 0.5 volt drop at full-rated generator current

Disconnect Plug Designation	Generator Circuit
A	*F+*
B	*A+*
C	Vacant
D	*A−*
E	*A−F−*

Minimum Brown and Sharpe Cable Size

Generator Type	*M-2*	*O-4*	*P-2*
Circuit 1	16	16	14
Circuit 2	8	4	0
Circuit 3	18	18	18
Circuit 4	16	16	16

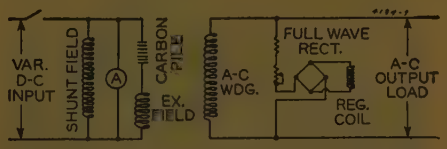


Figure 7. Motor-alternator set and regulator

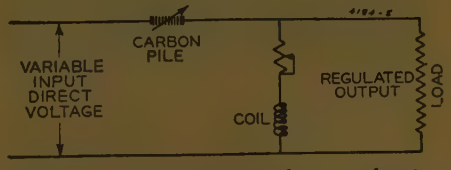


Figure 8. Line-voltage-regulator application

the engine but will allow the engine to operate up to its safe maximum output. The same results can be obtained by designing the generator so that its maximum output is slightly lower than that of the engine.

Motor–alternator sets are widely employed as power suppliers for some special application, usually radio. Here the exciter field is separately excited by the input voltage to the motor and is connected in series with the carbon pile. Output alternating voltage is regulated by connecting the regulator coil across the output of a full-wave rectifier. Some methods employ a step-down transformer, whereas others merely consist of resistance connected in series with the rectifier. Weight saving in the latter case is a large factor. Carbon-piler regulators have been used as frequency regulators in motor–alternator applications where frequency must be closely regulated. Constant frequency is obtained by regulating the speed of motor.

Line-voltage regulators are used in applications where the line voltage varies with loads, and so forth, as in the case of the electric system of an airplane, and it is desirable to maintain some value of voltage (below the minimum input variation) at a constant value. Such a load, requiring a constant input voltage, might be a filament load of radio tubes or other instruments whose accuracy depends upon its input voltage held constant. In these systems, the carbon pile is connected in series with the load, and the regulating coil circuit is connected across the load.

From what has been said, it is obvious that before selecting a regulator for a particular application, it is necessary to know

(*a*). The regulated voltage plus or minus allowable limits.

(*b*). Maximum continuous carbon-pile wattage dissipation.

(*c*). Maximum and minimum resistance.

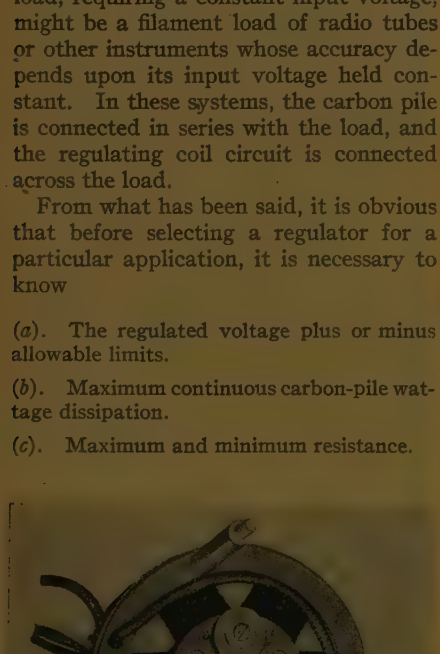


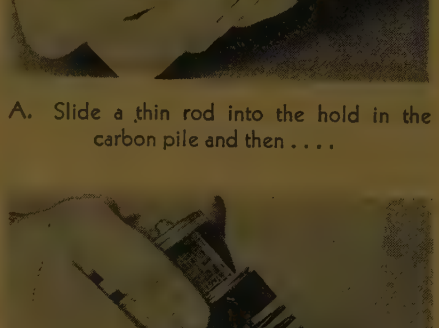
Figure 9. Position of armature assembly and connector lead in magnet case

View shows armature assembly assembled in magnet case. Center portion is carbon button which connects to the moving end of the carbon pile

The last two can be determined readily by employing a manual rheostat in place of the carbon pile in the control circuit making a complete test on the unit and calculating the requirements of the rheostat. Final carbon pile will have to have the same requirements as the rheostat It should be kept in mind that a high maximum pile resistance necessarily means a high minimum pile resistance, the practical limit of the maximum-to-minimum resistance ratio in most cases being about 30 to 1. In every instance, the test of the final selection of a regulator should be the successful operation on the equipment with which it is to be used. Many other considerations must be taken into account, such as matching time constants of a particular regulator with the time constant of the unit with which it is to be regulated. The fact that a regulator will operate satisfactorily with one unit does not necessarily mean that it will operate properly with a unit of a different design but having the same regulating resistance requirements. Reactance voltage, commutation, and time constants all enter into the picture and must be considered. Their effect can

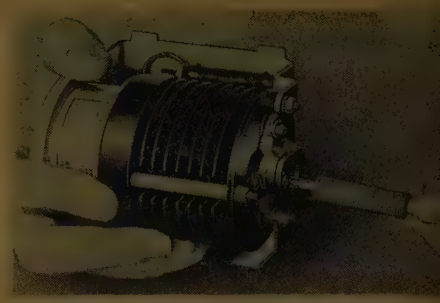


A. Slide a thin rod into the hold in the carbon pile and then . . . .



B. Invert the regulator to allow the disks of the carbon pile to fall out on the rod

Figure 10. Removal of carbon disks from carbon-pile regulator



A. With regulator horizontal, insert end of rod carrying disks of carbon pile into the ceramic tube then . . . .



B. Turn regulator up to a vertical position and allow the disks of the carbon pile to fall into the tube. Do not touch the disks with the fingers

Figure 11. Assembly of carbon-pile disks into ceramic tube of regulator

be determined only by actual tests.

Although the regulator is rugged in its construction and can stand much abuse, it must be remembered that it is primarily an instrument. The armature assembly has mass and is not counterbalanced (as all attempts to do so have resulted in a sluggish regulator) and is, therefore, sensitive to shocks and violent vibration. Proper consideration given to its mounting and location with respect to violent shocks and vibration will pay large dividends in its useful life, and trouble-free operation. Shock mounting in areas away from center of vibration should be the rule.

Advantages and Disadvantages

Certain disadvantages or shortcomings are experienced with the present carbon-pile regulator, as follows:

1. The regulator has three adjustments; this number will be reduced in future designs for simplification purposes.

2. Repairs and adjustments should be made by trained personnel at bases where reliable test equipment and accurate instruments are on hand.

Higher-Voltage D-C Aircraft Electric Systems

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Synopsis: Many of the advantages of 115-volt d-c aircraft electric power are presented with frequent comparisons to 24-volt direct current and 208/120-volt 400-cycle three-phase alternating current. System reliability is emphasized. Weight comparisons favorable to 115-volt direct current are shown for power generation, distribution, and load components. Problems concerning the use of the two high-voltage systems are discussed and compared. The popular belief that 115-volt d-c switching and commutation is unsatisfactory at high altitudes is refuted. It is concluded that 115-volt direct current is the lightest and most practical of the proposed electric systems for both medium and large airplanes.

CURRENT discussion of large aircraft electric systems has been carried on mainly in terms of commercial power distribution experience. There has been a tendency in such discussions to make blanket statements concerning the superiority of a-c systems without due consideration of direct current or aircraft-design requirements. Extrapolation of commercial practice and experience out of the field of its origin into aircraft design remains to be proved. The airplane presents a very special electrical design problem. Only when a-c systems are applied to actual aircraft designs, and after a careful competitive check made of both a-c and d-c results, are valid conclusions possible. A few facts learned from one such study on an actual airplane will be presented in this paper.

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Weight and Safety

Reliability and weight are the principal determinants of aircraft electrical design. These factors are far more important in aircraft than in other fields of electrical engineering. One of the large air-transport companies has estimated the value of weight saved over a five-year operating period at $600 per pound per airplane. In today's larger airplanes electricity has acquired many new responsibilities, such as engine controls, flight-surface controls, landing flaps, and landing gear, many of which are essential to flight. Thus, the design of the electric system may be literally a life or death matter.

The reliability of an aircraft electric system is a function of weight and engineering experience. Currently proposed higher-voltage a-c and d-c systems for large aircraft may be made equally reliable. However, the weight and accumulated experience necessary to achieve this result will be greater for the a-c than for the d-c system. Some of the proposed a-c systems have secured a favorable weight by sacrificing present standards of reliability. Any higher-voltage system which does not provide at least the same degree of safety as present low-voltage d-c systems employing main-engine-driven parallel-operated generators is not comparable and is not really suitable for aircraft application. The 115-volt d-c system herein discussed is intended to exceed considerably existing standards of reliability in order to assume the new electrical responsibilities. Basic requisites of any aircraft electric system includes such factors as:

1. Maximum system stability under high fault loads.
2. Optimum fault burn-clear characteristics.
3. Uninterrupted full-load capacity during momentary fault conditions.
4. Continuous uninterrupted full-load capacity in the event of failure of one main power source.

System Types

Three principal electric systems are at present receiving extensive design consideration:

1. The established standard 24-volt d-c system.
2. The 115-volt d-c system, which is the subject of this discussion.
3. The 208/120-volt 400-cycle three-phase a-c system which recently has received considerable attention.

If as much study, research, and development had been directed toward higher-voltage d-c aircraft power as has been applied in the past two years to a-c aircraft power, it is believed that the future standard would be the higher-voltage direct current.

On any given airplane a small amount of 400-cycle alternating current or 24-volt direct current may be required, but this does not constitute a significant factor in the design of the main power generation and distribution system.

Functional Breakdown of the Electric System

For the purposes of this discussion the aircraft electric system is functionally separated into three general divisions: (1) distribution, (2) generation, and (3) the load component parts.

The weight of the load component parts of an aircraft electric system is the least effected by the voltage or type of system employed. Thus for a given airplane the total weight of the motors, lights, switches, and miscellaneous equipment may be substantially unchanged for 24-volt direct current, 115-volt direct current, or 208/120-volt 400-cycle a-c designs. While small differences in the weight efficiencies of certain items on the different systems will be apparent, the over-all total weight differences are not likely to be decisive.

The weight of the generative portions of the aircraft electric system will be lighter for 115-volt than for 24-volt direct current, but both will be substantially lighter than comparable 400-cycle a-c designs.

The most important weight differences appear in the distribution system. Large

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3. A minimum resistance is ever present and varies with the maximum resistance with the obvious disadvantage that it never can reach zero.

Advantages of the carbon-pile regulator are as follows:

1. The carbon-pile regulator is capable of regulating high currents.
2. The carbon-pile regulator does not contain any movable contacts to get out of order. These were always a source of trouble due to short life, causing radio interference, susceptibility to high altitudes, and a constant maintenance problem. When properly adjusted, the carbon pile gives a smooth change in resistance.
3. There are practically no moving parts to wear out or get out of order. The only parts requiring replacement are the carbon pile and contact plugs. With the proper replacement of these parts and readjustment, the regulator is as good as new.

Few people can realize what an important part in our war effort this amazing instrument performs. Its true value probably will not be fully known until after the war, when many of its present-day applications will be converted to peacetime use to make commercial aviation safer.

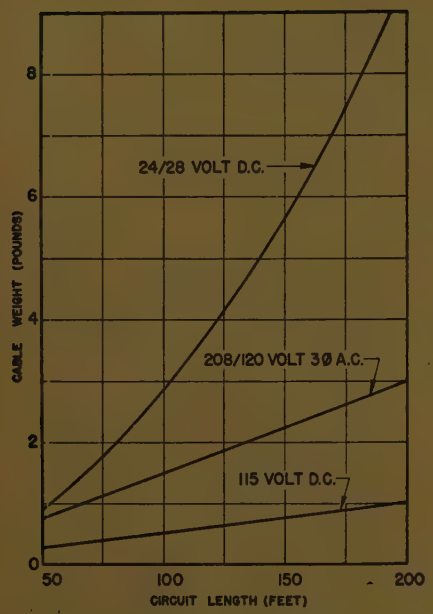


Figure 1. Aircraft cable weight for transmitting one-fourth horsepower

savings in weight with respect to low-voltage direct current are effected by both of the high-voltage systems; however, the 115-volt d-c distribution system is by far the lightest.

Degree of Electrification

Weight analyses for the three electric systems is difficult because of the large variation possible in the degree of electrification in airplanes of the same general type and size. The weight differences between the three systems vary greatly depending on whether or not heavy load items, such as landing gear, landing flaps, flight-control boosters, engine and ventilation controls are electrically operated. Electric-system designs for the same airplane may have a three-to-one variation in weight due to the degree of electrification.

In present 24-volt airplanes the large variation in degree of electrification is because, in many designs, there is no decisive weight advantage for either electricity or hydraulics. However, with higher voltage the weight advantage of electric operation may be large because of the saving in wiring weight, particularly on the large airplanes. On such airplanes the wire weight to an item at 24 volts frequently exceeds the weight of the item itself.

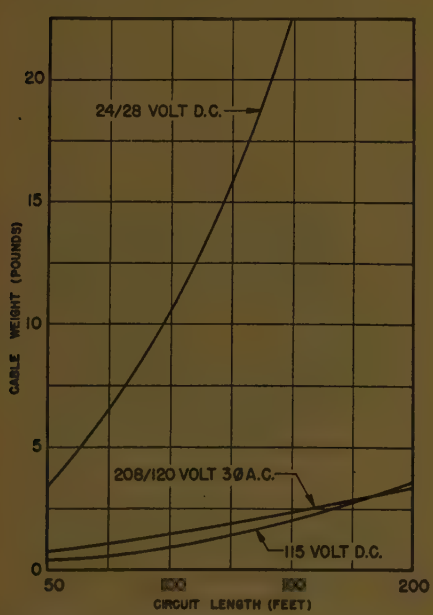


Figure 2. Aircraft cable weight for transmitting one horsepower

Power Distribution

Weight Savings of 88 Per Cent

The weight for transmitting one horsepower 100 feet over a single-wire-grounded aircraft system with a five per cent allowable voltage drop, or 50 feet over a two-wire circuit, amounts to ten pounds with 24-volt direct current but amounts to less than one pound with 115-volt direct current. Figures 1, 2, and 3 show the relation between weight, distance, and horsepower for the various systems. On large airplanes much of the heaviest wiring, that from main engine-driven generators located in the wings, propeller feathering pumps, empennage turret guns, rudder and elevator boosters, landing flaps, and landing gear can be reduced to 12 per cent or less of its present weight by changing from 24 to 115 volts.

Conductor Weight

Voltage is the principal factor determining the weight of the distribution system. Metallic conductor weight in a d-c system varies with respect to voltage:

1. Inversely as the square of the voltage for those conductors determined by voltage drop.

2. Inversely as the voltage for conductors determined by current carrying capacity.

Reduction in cable weight can be carried only as far as the minimum conductor gauge (at present number 22). Thus, a change from 24 to 115 volts can result in a weight reduction inversely proportional to the square of the voltage for wires originally number 10 gauge or larger. Smaller wires, determined by voltage drop, will be reduced to the minimum size before a 16 to 1 weight reduction can occur. Similarly, a reduction to one fourth of the 24-volt-system weight, for conductors determined by current carrying capacity, will be limited to number 16 gauge and larger.

Cable Insulation Weight

The insulation of some aircraft cables appears to be adequate for any of the higher-voltage systems now under consideration. In terms of voltage change the conductor insulation weight varies:

1. Inversely as the voltage for wires determined by voltage drop.

2. Inversely as the square root of the voltage for wires determined by current carrying capacity. Cable insulation weight varies with wire size from ten per cent of the total cable weight for number 00 to 50 per cent for number 22.

Conduit Weight

Voltage, as it affects cable size, is also a major factor in determining the weight of many other accessories, such as, cable terminals, splices, connectors, junction boxes, and conduit. In terms of voltage, conduit weight will:

1. Vary inversely as the voltage for cables, the sizes of which are determined by voltage drop.

2. Vary inversely as the square root of voltage change for cables determined by current carrying capacity.

3. Not vary for instrument and other cables already containing minimum-size conductors.

Examples of 24-Volt Weight Inefficiency

A weight analysis of the 24-volt electric system of a 50,000-pound gross-weight four-engine transport indicated that:

1. Seventy-five per cent of the total conductor weight was determined by the five per cent voltage-drop limitation.

2. Five per cent of the total conductor weight was determined by current capacity limitations.

3. Twenty per cent of the total conductor weight was not affected by voltage change.

4. Seventy per cent of the total cable weight was estimated to be metallic conductor, and 30 per cent to be insulation.

Thus the weight efficiency of the 24-volt d-c system is poor even on present airplanes. In the example just cited only seven per cent of the conductor weight of cables larger than minimum size was operating at maximum current carrying capacity. Even two-engine airplanes in the 20,000-pound gross-weight class will have less than 50 per cent of the conductor weight of cables above minimum size operating at maximum capacity. For good weight efficiency it is desirable that aircraft electric-system voltage be high enough, so that substantially all of the wiring is either operating at maximum current carrying capacity or is reduced to the minimum allowable size.

In designing 24-volt electric systems for some of the really large airplanes now under construction with gross weights of several hundred thousand pounds, it is found that less than one per cent of the conductor weight of cables above minimum size would be operating at maximum current carrying capacity. It is believed that the magnitude of the weight penalty paid for use of present 24-volt aircraft

electric systems is not generally appreciated. On the four-engine transport airplane previously mentioned it was estimated that more than 100 pounds of wiring alone could have been saved by designing for 115-volt direct current.

The wiring-system weight of one large eight-engine airplane was calculated for two different designs. With a maximum degree of electrification the electric cable and bus weight totaled 2,190 pounds for 24-volt and 906 pounds for 115-volt direct current, a saving of 1,284 pounds. With a minimum degree of electrification the electric conductors had a total weight of 1,100 pounds for 24 volts and 600 pounds for 115 volts, a saving of 500 pounds.

Comparison of A-C and D-C Cable Weight

Actual design figures indicate that three-phase a-c power-distribution systems will weigh considerably more than comparable d-c systems for the following reasons:

1. Currently proposed a-c systems require three conductors for each three-phase circuit. Only a single conductor is needed in commonly used grounded return d-c systems. Application of three-phase a-c power to circuits already reduced to minimum size in 115-volt d-c designs will result in a three-fold increase in weight for the wiring of these circuits. Increasing the present electric system voltage approximately four times, from 24 volts to 115 volts, will reduce to minimum size more than 50 per cent of the total cable weight of even our largest airplanes of several hundred thousand pounds gross weight. While a considerable portion of this minimum-size wiring will be for single-phase lighting and instrument loads, an increase in total cable weight of 40 per cent or more can be expected from this cause when three-phase four-wire grounded-neutral a-c systems are compared with equivalent grounded-return d-c systems.

2. Power factors from 40 to 70 per cent are expected for 400-cycle motors and other inductive items. It is estimated that additional weight for power-factor correction or increased cable size will raise the total cable weight from 5 to 10 per cent.

3. Connectors, terminals, conduit, and other devices incidental to the power-distribution system, of course, will be lighter for a single-wire grounded-return d-c system than for a three-wire three-phase grounded-neutral a-c system. Roughly, the total estimated weight of these sundries for one specific design was 20 per cent more for alternating current than for 115-volt direct current.

On airplanes over a hundred thousand pounds gross weight, the weight of three-phase grounded-neutral a-c power distribution probably will exceed by 50 per cent the total weight of a single-wire grounded-return d-c system, voltages being the same. At present even the 208÷120-volt three-phase wiring weight is considerably heavier than that for the 115-d-c equivalent.

In the future, when airplane designs require several hundred kilowatts of electric power, it may prove desirable to use 230-volt d-c or perhaps 230/115-volt d-c systems. While potentials higher than 115 volts have only a small weight advantage at the present time, they do introduce insulation and switching problems.

This study indicates that 115-volt direct current offers weight savings even for small airplanes of 25,000 pounds or less gross weight. This is not true of three-phase alternating current, since the relative weight disadvantage of three-phase a-c power distribution increases rapidly with decrease of airplane size.

Comparison of A-C and D-C Distribution-System Reliability

Safety of any system or device is largely a function of its simplicity. The single-wire d-c aircraft distribution system certainly achieves the optimum of simplicity. Statistically, it can be shown that protective devices, fuses, circuit breakers, and relay systems can be carried beyond the point of optimum reliability. Every protective device added to a circuit or system carries with it the failure liabilities inherent in that device.

On main distribution lines and feeders it is desirable to provide multiple fault protection within the lines. This practice has for more than a decade been used in industrial and municipal power-distribution systems. Figure 4 illustrates a simplified three-wire multiple fault-protected feeder system of familiar design. It will be noted that a fault at point X would be fed by parallel fuses A, B, C, and D in series with the faulted line's single fuses E and F. It is obvious that fuses E and F, each fed by two units in parallel, would blow, clearing the line of this fault. This type of circuit protection is of doubtful value in low-voltage systems, since contact-resistance and slight-resistance inequalities of the feeder branches will cause unequal current distribution in the feeder lines sufficient to upset the effective fault discrimination of the circuit. At higher voltages these same factors have a negligible effect on the protective action and fault discrimination of the circuit. Not only does this provide increased safety and reliability for all main feeders, busses, and essential circuits, but also, with a single-wire 115-volt d-c design little of the essential protection of this system is lost by the added complexity. The case is very different when the same type of three-wire multiple fault protection is attempted in a three-phase four-wire a-c distribution system. In such circuits the number of protective devices would be multiplied by three over those required for direct current. It is doubtful whether a three times increase in complication and circuit-protector inherent liability would result in an overall safety gain. Many essential circuits having minimum-sized wire can be protected justifiably in a single-wire d-c system.

Both safety and cost of airplane operation are affected favorably by the simplicity of the single-wire d-c distribution system. Present service and design and manufacturing personnel would have to be trained especially for the safe operation of a-c aircraft systems. The application of 115-volt d-c aircraft power would involve no essentially new techniques nor operational hazards.

Circuit-Interrupting Equipment

Design Fundamentals

Interrupting equipment for 115-volt direct current can be lighter than existing 24-volt direct current or 208-volt three-phase alternating current.

The design parameters of 24-volt direct current, 115-volt direct current, and 208-volt alternating current for aircraft switches differ radically. The widespread belief that switching of 115-volt direct current is difficult results from unsatisfactory performance of certain available 24-volt equipment when tested at 115 volts. Assertions that adequate 115-volt d-c switches will compare in size and weight with the conventional industrial counterparts are incorrect. It is also improper to expect a-c equipment to perform satisfactorily on direct current.

A low-travel snap-action-type switch, having a 0.070-inch contact gap and rated ten amperes at 24 volts direct current, interrupts less than one ampere at 115 volts direct current. Also, an AN-3023 aircraft toggle switch rated ten amperes at 24 volts interrupts less than one ampere at 115 volts. Actually, three amperes in a 115-volt d-c inductive circuit will support an arc across a one-eighth-inch gap. Such performance has discouraged the adoption of 115-volt d-c aircraft power. This attitude is not justified, since well-known design principles can and have been applied to solve this problem. These switch-design principles include:

1. Multiple break.
2. Deion grid.
3. Magnetic arc suppression.
4. Solid insulation arc barrier.
5. Liquid arc barrier.
6. Hermetical sealing.

(*a*). Pressurized gases.
(*b*). Vacuum.

Sufficient empirical data have been compiled which definitely disprove many statements made concerning switch inadequacy for the higher-voltage direct current.

115-Volt D-C Switches Are Available

For immediate application the Cutler Hammer Company offers a line of 110-

220 d-c "tool-handle" switches, which have 5-, 10-, and 20-ampere industrial ratings. Actual tests have indicated that these switches may be used at approximately half of their commercial rating for altitudes up to 40,000 feet when connected as single-pole double-break units. These switches are designed to interpose a solid insulation barrier between the contacts, which effectively suppresses the arc. For equivalent power interruption these switches are lighter than the conventional 24-volt line.

A double-pole toggle-switch type *AN*-3023 has six times the single-pole capacity when the two poles are connected in series for a double break.

Low-travel snap-action-type switches are available with magnetic-arc-suppression provisions and are rated ten amperes at 115 volts direct current for operation up to 40,000 feet. One such switch was tested with and without the magnet from sea level to 50,000 feet with various contact spacings. The capacity of the switch was increased by 400 per cent with the proper use of the arc suppressing magnet. It is possible to use a small magnet for arc-extinguishing purposes for as little as one tenth of an ounce additional weight per switch. Within the aircraft industry it has been stated that a magnetic field is ineffective as an arc suppressing agent at low pressures, and that arc reversal occurs at certain altitudes. A careful check was made under altitude simulated conditions up to 50,000 feet. All of the mentioned altitude irregularities of devices incorporating magnetic arc suppression were experienced, but on investigation proved to be due to inadequate insulation and spacing. The tests performed indicate that accepted theories of arc behavior in magnetic fields are correct.

The 24-volt type *AN*-3022 aircraft toggle switch may be used safely for three-fourths of an ampere at 115-volts direct current for altitudes up to 50,000 feet. At 115 volts this is 90 watts, and the switch should satisfy more than 40 per cent of the total switch requirements.

A study of airplane switching requirements indicates that 90 per cent of the loads on a large airplane will require a three-ampere switch for 115 volts and a 15-ampere switch for 24 volts.

Three-Phase A-C Switching

The three-pole switch for three-phase alternating current is inherently heavier and more complicated than the single-pole switch commonly used in d-c circuits.

Switch Developments

The preceding discussion concerns conventional switch design. An important electric function, such as aircraft switching, demands equipment designed specifically for the purpose. A great deal of effort has been expended in the last two years in the attempt to interest aircraft-accessory manufacturers in the design and production of hermetically sealed switch gear. Obviously, a switch with contacts sealed in an enclosure with hydrogen or some other suitable gas under pressure would not be deleteriously affected by altitude variation, humidity, moisture, ice, dust, foreign matter, contact oxidation, and ionization. A small hermetically sealed switch manufactured by the Western Electric Company for use in submarine cables was tested with encouraging results. This switch, measuring one quarter of an inch in diameter and two inches in length, had magnetic leaf-type contacts enclosed in a hydrogen atmosphere. The switch was actuated by an external magnet and satisfactorily interrupted more than one ampere at 115-volts direct current in a circuit having an L to R ratio of 0.020. This same switch is adaptable to electromagnetic operation as a relay. At the present time higher-capacity 15-ampere sealed switches and relays with double-throw contact arrangements are being developed by several companies.

Relays

In general the remarks concerning switches apply as well to relays. A line of relays originally designed for three-phase 208-volt alternating current recently has been given 115-volt d-c ratings. They are available in three sizes up to 5-kw continuous duty and 20-kw rupturing capacity. For d-c use the ratings are based on series connection of the three poles, thus constituting a single-pole relay having a six-break mechanism.

Another relay originally designed for 24-volt direct current has been adapted to 115-volt d-c service by the addition of an 0.05-ounce Alnico magnet for each pair of contacts. Tests indicated that this relay would interrupt safely ten amperes at 30,000 feet and six amperes at 50,000 feet.

It is interesting to note that an industrial-type relay rated 100 amperes at 115 volts direct current weighed 25 pounds, while a comparable aircraft relay designed for the same voltage weighed only 1.75 pounds. Assertions that higher voltage will result in extremely heavy switch gear probably are founded on the consideration of such commercial equipment. It should be noted that the aircraft units were designed to much more severe requirements for an ambient-temperature range of +158 to −85 degrees Fahrenheit, and for vibration and acceleration loads in excess of ten grams. In fact, this 115-volt relay weighs less than a 24-volt relay having a comparable power rating. The weight of a high-current 24-volt contact mechanism is inherently heavier than that for the lower-current 115-volt power equivalent. Specifically, the weight of current carrying components is substantially greater than that of arc suppression mechanisms.

Circuit-Protection Devices

Circuit breakers incorporating electromagnetic arc extinction have proved satisfactory for 115-volt aircraft circuit protection. Design investigations are being conducted to adapt these circuit breakers to overload protection and circuit-switching service. The General Electric Company has a series of 208-volt a-c 115-volt d-c fuses which may be preferred to circuit breakers.

Lighting

Filament Characteristics

In the design of aircraft 24-volt lamps for low-wattage sizes, the filament has been drawn to minimum diameter and in some cases has been etched to reduce further the cross-section area. When it is suggested that lamps be designed for 115-volt service, it is argued that filaments are already too fragile. In many instances these low-wattage lamps give insufficient light and would be better replaced with brighter lamps. To get this higher output the filament need only be increased in length, the diameter being the same. In increasing the length it is assumed that the filament support will be of a sturdy vibration-resistant character. The filament-diameter worry exists only with the low-wattage low-lumen output lamps. The filament of a 50-watt 115-volt lamp could be the same diameter as the widely used 15 candle power 24-volt aircraft lamp. The increased efficiency of the electric generating system plus the reduced power loss in high-voltage distribution systems, since much of the cable already will be of minimum gauge, permits the use of higher-wattage lamps. The problem with 115-volt landing lamps is with the filament support rather than with the filament diameter. Several 115-volt lamps with different-type filament supports have been tested under simulated aircraft-vibration conditions. Certain lamps which have especially designed vibration-resistant filament supports operated for at least 250 hours when vibrated over a range of 100 to 3,600 cycles per minute with a total excursion of 0.031 inch. Therefore, it is concluded that satisfactory lamps are available in 20-, 40-, and 50-watt sizes.

Lamp Life Affected by Mounting

Where lamp life is important the lamp-assembly mounting should be designed carefully. An actual lamp installation was mounted on a platform and vibrated with a 0.031-inch total excursion. The filament movement was measured to be from 0.062 to 0.25, depending on the resonant characteristics of the mounting. This is mentioned because lamp failure is frequently the result of faulty installation rather than lamp design. Shock mounting of lamp assemblies may be desirable to increase the life of certain essential lamps such as landing lights.

A-C Lamps

Filament resonance, sometimes encountered with a-c lighting and especially with 400-cycle power, does not exist with direct current. In order that phase balance may be maintained during lamp on and off conditions it is necessary that high-wattage lamps for alternating current have special three-phase filament structures. For smaller lamps it is general practice to distribute lamp loads among the three phases with resultant wiring and phase-balance complication.

Present Supply Problems Temporary

The aircraft electrical designer is having difficulty in obtaining a wide variety of adequate 115-volt lamps. This is probably due to the high production demands on lamp manufacturers because of war necessity.

Radio

It is frequently stated that alternating current is ideal for radio power, while direct current, because of voltage-conversion difficulties, is not. This is a fallacy, because fundamentally radio equipment requires direct current which is normally obtained from alternating current by rectification and filtering. The aircraft radio designer seldom chooses tubes for desired d-c potentials but instead resorts to dynamotors, inverters, and transformers. This procedure is neither desirable nor necessary. At present 24-volt direct current is being used successfully as the only operating potential in some aircraft radio receivers and in at least one transmitter, except for the power stage. Equipment so designed has proved the equal of previous designs in all respects and superior as regards to voltage breakdown of capacitors and insulation. A d-c potential of 115 volts can be used with greater facility and even better results. Beam-power amplifiers represent a step in the direction of low-voltage transmitting tubes.

Motors

General Considerations

On large airplanes having a gross weight of over 100,000 pounds, 150 or more motors may be installed. On one well-known airplane at least 125 motors are used for armament alone. Although motors represent as little as 20 per cent of the airplane electrical equipment, they may constitute as much as 75 per cent of the entire electric load.

Motor weight is a function of the performance required. In general the weight of d-c motors for high-starting and peak-torque service will be less than for alternating current. For example, a peak output of eight horsepower will require a two- or three-horsepower d-c motor or about a four-horsepower a-c motor. On the eight-engine airplane previously referred to, about 70 per cent of the motors were estimated to be of the high-torque intermittent-duty type.

Motor Efficiency

Efficiency of intermittent-duty equipment is not important, since power loss due to inefficiency of such equipment is negligible. The weight of intermittent-duty equipment can be reduced by designing for lower efficiency, and such weight saving usually will more than justify such design. Only where the peak horsepower requirements of a motor approach the peak generation output of the aircraft electric system is efficiency of intermittent-duty motors a critical factor.

The efficiency of a 115-volt d-c motor will be higher than that of a comparable 24-volt motor due to lower brush losses. For a given power output a 115-volt d-c motor will be lighter than an equivalent low-voltage motor by virtue of smaller commutator and brushes.

Brushes

A great deal has been said recently concerning brush wear. Many conflicting theories have been expounded for the erratic performance of brushes at high altitude. It is not within the scope of this discussion to review or conjecture on the suppositions now prevalent. Excessive brush wear at high altitudes usually has been evident where current densities have approached 200 amperes per square inch.

As a result of current military need for improved 24-volt brush life, research and development have progressed to the point where 1,000 hours' service has been assured for motors and generators from sea level to 40,000 feet. The same techniques will be applied to 115-volt d-c rotating equipment with even longer brush life. In the near future brush maintenance on d-c motors may be almost negligible in view of the development of new brush materials.

Explosion-Resistant Motor Requirements

Identical explosion-resistant requirements exist for both a-c and d-c, high or low voltage motors. The assumption that brushless motors are explosion proof is unjustified since they are equally susceptible to:

1. Faulty workmanship.
2. Insulation breakdown.
3. Stalled rotor overheat.

Any of these faults may result in ignition of explosive fuel mixtures.

Power Generation

Altitude Effects

Military demands have increased the altitude ceiling of airplanes to 40,000 feet or more. This does not mean that such high-altitude flight will prevail after the war. Airplanes may fly as high as 25,000 feet in order to avoid storm conditions, but it is probable that commercial transport and cargo planes seldom will be flown above 20,000 feet. Direct current is generally conceded as being satisfactory for electric power up to 25,000 feet, but it is claimed that alternating current is the only answer for very high altitudes. Lengthy technical papers have been published in which higher-voltage d-c system considerations were dismissed with a few perfunctory remarks to the effect that higher-voltage d-c power is not suitable for high-altitude service. Experience and test results indicate that direct current is not only adequate, but may be the best type of very high-altitude aircraft power. Satisfactory performance of d-c inverters, radio dynamotors operating at 1,000 volts or more, and electric fuel pumps on airplanes being flown at extremely high altitudes substantiate this statement. The a-c systems now under consideration employ a d-c generator for alternator excitation. This practice seems inconsistent if d-c generation is not considered entirely reliable at high altitudes. Objection to the use of direct current at high altitudes has come from the improper use of equipment of doubtful low-altitude design at 30,000 to 40,000 feet. The failure of marginally designed equipment, such as generators having brush current densities of over 200 amperes per square inch, does not constitute valid reason for objection to the

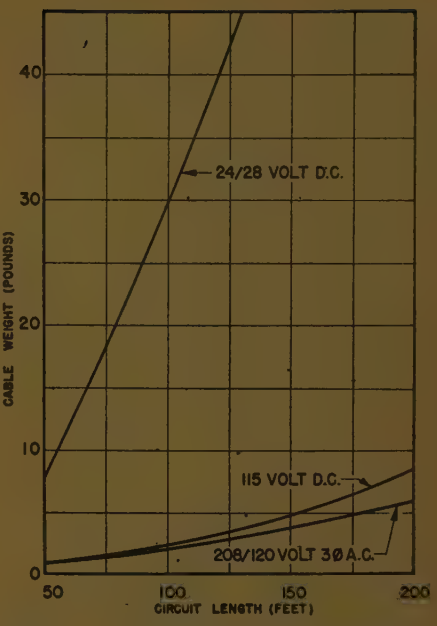


Figure 3. Aircraft cable weight for transmitting three horsepower

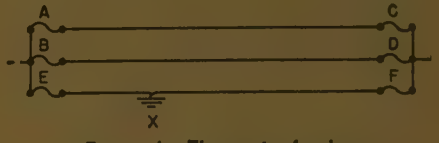


Figure 4. Three-wire feeder

use of direct current for high-altitude airplanes. Conservative design which incorporates maximum compensation, normal brush current densities, and other standard commutation practices will result in good high-altitude performance.

High- and Low-Voltage D-C Weight Comparison

The weight of 115-volt generation equipment will be less than that for 24-volt design. Smaller brushes and commutator area make the 115-volt generator lighter than comparable low-voltage machines. High-current low-voltage commutation is heavier and less reliable than the low-current 115-volt equivalent.

On very large airplanes it would be impractical to parallel ten 24-volt 6-kw generators for a required power of 60 kw. Neither is it practical to design larger generators having an output of 1,000 amperes or more at 30 volts because of the commutation difficulties and unwarranted commutator weight. Present 115-volt generators are at least 15 per cent lighter than equivalent 24-volt units. Appreciable weight savings are obtained for the 115-volt d-c voltage-regulation equipment. On the same eight-engine airplane it was estimated that the high-voltage generation system was lighter than the low-voltage generation system by approximately 150 pounds.

A-C and D-C Weight Comparison

There should be little difference in weight between comparable a-c and d-c generators when the necessary alternator field excitation is included. Comparison of constant-speed alternators with variable-speed d-c generators is confusing and wrong. The important weight difference between a-c and d-c machines comes from generator drive requirements. A variable-speed d-c generator is very much lighter than the combination of an alternator and its associated constant-speed drive mechanism. The speed of airplane propulsion engines is varied from approximately 1,000 to 3,000 revolutions per minute. To maintain constant frequency within practical limits it is necessary to interpose an automatic constant-speed transmission to drive the alternator from a main engine. The weight of a variable-speed d-c generator is somewhat more than that of a constant-speed generator, but this weight difference is small compared with the weight of a constant-speed drive mechanism required for constant-frequency alternating current. This constant-speed transmission device is proving to be a serious design problem. It is expected that the weight of these constant-speed drives can be reduced approximately to two pounds per horsepower for the 60-kw alternator. In addition to an estimated 150-pound weight penalty the constant-speed drive creates unknown hazards and complication.

The difficulty with a-c design does not end with securing relatively constant frequency. There is the additional problem of providing means for paralleling the alternators. The relative position of paralleled 400-cycle alternator rotors must be the same with only a very small angular displacement permissible. Equipment to accomplish such synchronization is usually complex.

A method of avoiding parallel operation of alternators is to divide the airplane loads between two independent alternators with automatic means for transferring the loads from an alternator, which may fail, to the remaining one. The divided load system will require more elaborate distribution wiring and bus installations, more extensive load protective measures, and complicated transfer equipment. The load transfer system to be practical must be automatic and sufficiently rapid, so that there is no appreciable interruption of service. There should be no danger of stalling motors, of de-energizing relays, nor of interrupting solenoid-operated equipment. A load transfer system is usually troublesome, and its design is technically difficult. The reliability of such a system is questionable and cannot be compared with the inherent safety and simplicity of parallel-operated d-c generators.

For alternating current, a divided load system on large aircraft will require two alternators driven at a constant speed, each of which must have sufficient capacity to carry the entire load. The size and weight of the alternator and constant-speed-drive combination does not permit direct mounting on a main engine. A remote accessory drive shaft and gear box would be required, and it is to be remembered that space aft of the engine is at a premium. A proposed remote drive for each 30-kva alternator was estimated at 50 pounds, a weight penalty which a-c systems can ill afford. If it is assumed that a large airplane will require 60 kva, the generation system will have 120 kva available in two alternators. For the same requirements six main engine-driven 15-kw d-c generators can be used with much greater reliability. The six d-c generators having 90-kw output will be considerably lighter than the 120-kva alternator installation.

Auxiliary Engine Power

Auxiliary engine-driven aircraft alternators have been operated successfully in parallel, and this system constitutes a standby until the proposed systems are proved satisfactory. The reason for present consideration of other a-c power systems is the weight inefficiency of constant-speed auxiliary electric drives which will be explained.

Auxiliary power may be the answer to many electrical design problems, but the efficient use of such power cannot be obtained where constant speed operation of the auxiliary engine is required. This is because the average electric load for long flights may be less than 30 per cent of the peak demand. The fuel consumption may be reduced only 36 per cent for a 70 per cent reduction of load. Another cause of high auxiliary engine fuel consumption is the fact that, to compensate for the loss of power at altitude and to supply customary short-time overload capacity, the auxiliary engine size must be increased considerably over the nominal requirements. For a required 50-kw peak load at 20,000 feet, a 175-horsepower auxiliary engine is needed. Of this 175 horsepower an average of only 15 kw will be used in flight, yet 64 per cent of the full-load gasoline consumption will be required. An estimate of fuel required by two 50-kw auxiliary engines operated at constant speed throughout a 25-hour flight was 600 pounds more than for main engine-driven generators.

Fuel consumption of auxiliary engines can be reduced materially by governing engine speed in conformity with load requirements. This may be accomplished by providing a load-sensitive speed governor for the auxiliary engine, driving a variable-speed d-c generator. A two-to-one speed regulation of the auxiliary from peak load to average load is required to show favorable weight comparison with present main engine-driven designs. Load governing of the auxiliary between wider limits would result in additional weight savings. It is obvious that the advantages of variable-speed load governed auxiliary power-plant design are practical only with the use of direct current.

Where auxiliary power plants are installed for engine starting and for on-the-water operation, it is desirable that they also be used in flight. Because of vibration complications, service troubles, and crowded accessory compartments, it is desirable to remove the generators from the engine. By centralizing the power supply a material saving can be made in wiring and bus weight.

Battery Power

In the electric-system design there may be some need for limited amounts of stored energy which can be supplied by storage batteries. Depending on the capacity required, it may prove lighter to install a small auxiliary power plant in place of the storage battery. In either case the stored energy can be conveniently a part of a d-c system. The battery constitutes an extremely inefficient source of electric power, and its use will be avoided wherever possible.

Conclusion

For large aircraft, regardless of altitude service, 115-volt direct current is more practical than either 24-volt direct current or 208/120-volt 400-cycle three-phase alternating current. The 115-volt system is considerably lighter than either the 24-volt d-c or the a-c system.

Application of Electronics to Aircraft Flight Control

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ROBABLY no other control problem offers so many opportunities for the vantageous application of electronics the design of equipment for automatic ght control. The type *C*-1 Autopilot ed in the Army's heavy bombardment rcraft is a typical example of equipment which the use of electronic devices has oduced a flexible well-co-ordinated ntrol system from what was formerly unwieldy electromechanical machine.

herent Design Problems

To appreciate fully the problems in the sign of an automatic pilot it is first cessary to understand the fundamental inciples of flight control.

The control of an airplane can be re-lved about three perpendicular axes, illustrated in Figure 1. When the sition of the airplane is such that its ll and pitch axes are horizontal and its rn axis is vertical, it will fly "straight d level"; but, whenever the airplane made to rotate about one or more of ese three axes by movement of its lerons, elevators, or rudder, its flight th is changed. The ailerons, elevators, and rudder therefore are called the *control surfaces* of the airplane. It is important to remember that the position of these control surfaces does *not* determine the airplane's position, but rather the rate of the airplane's rotation about its corresponding axes. This characteristic of airplane flight control makes it necessary for the pilot to perform double operations in executing simple maneuvers.

As an example, consider the operation of the ailerons in making a left turn. First the airplane is made to roll to the left by displacing the ailerons—the left aileron upward, the right aileron downward. As the airplane reaches the desired angle of bank, the ailerons again are streamlined to stop further rotation, and the airplane remains in the bank until the desired turn has been made. The ailerons are then displaced in the opposite direction for the time required to return the airplane to level flight. Rudder and elevator controls are operated in a similar manner, except that the elevator and, to a lesser degree, the rudder are not completely streamlined during the maneuver.

Another complication in aircraft flight control arises from the fact that, in most turning maneuvers, the ailerons, rudder, and elevator all must be operated simultaneously to produce a proper turn. Too much rudder will cause a skid; too much aileron, a slip; and too little up-elevator will permit the plane to lose altitude in the turn. Therefore, any automatic pilot must accurately proportion the movement of each control to fit a complex pattern and, at the same time, co-ordinate the movement of all controls to maintain them in proper relationship with one another.

In addition, it is highly desirable for an automatic pilot to be so universally adaptable that identical units from the production line will operate effectively when installed in different types of airplanes. Since different types of aircraft have widely different flight characteristics, and since these same characteristics are influenced materially by speed, loading, altitude, and weather conditions, an automatic pilot, to be universally adaptable, must have provisions for easily adjusting its operating characteristics to match those of the airplane.

The use of vacuum-tube circuits not only simplifies the solution of these problems but also makes possible a degree of sensitivity and accuracy not obtainable by any other means. These features are of basic importance in an automatic pilot designed for high-altitude precision bombing.

Description of Autopilot Equipment

The primary function of an automatic pilot is to fly the airplane straight and level. It must operate the controls quickly and smoothly in response to any deviations from an established attitude and heading.

In the *C*-1 Autopilot the vertical flight

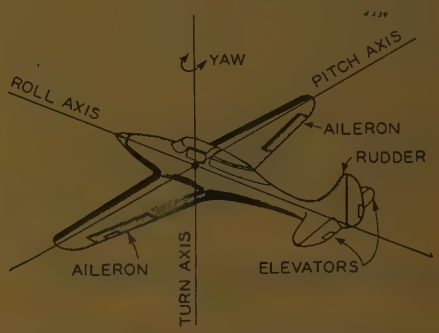


Figure 1. Flight-control axes of an airplane



Figure 2. Vertical-flight gyroscope

aper 44-239, recommended by the AIEE committee on air transportation for presentation at the IEE Los Angeles technical meeting, Los Angeles, alif., August 29–September 1, 1944. Manuscript bmitted June 14, 1944; made available for printg July 21, 1944.

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Although a few problems exist with the se of either high-voltage system, those r direct current are not difficult, while a tisfactory solution for the many a-c roblems is questionable.

eferences

D-C ARC INTERRUPTION FOR AIRCRAFT, J. S. uill, L. T. Rader. AIEE TRANSACTIONS, volume , 1944.

PEAK VOLTAGES WITH D-C ARC INTERRUPTION OR AIRCRAFT, Virgel E. Phillips, Walter P. Mitchel. IEE TRANSACTIONS, volume 63, 1944.

HIGH-ALTITUDE BRUSH PROBLEM, D. Ramaanoff, S. W. Glass. AIEE TRANSACTIONS, volume , 1944, November section, pages 825–30.

A-C AND D-C SHORT-CIRCUIT TESTS ON AIRAFT CABLE, J. C. Cunningham, W. M. Davidson. IEE TRANSACTIONS, volume 63, 1944.

ELECTRIC-CIRCUIT BURNING-CLEAR AND DAMGE PHENOMENA ON AIRCRAFT STRUCTURES, C. M. Foust, J. G. Hutton. AIEE TRANSACTIONS, volume 63, 1944, April section, pages 198–204.

6. A 120-VOLT D-C AIRCRAFT ELECTRIC SYSTEM, L. M. Cobb. AIEE TRANSACTIONS, volume 63, 1944.

7. DESIGN RELATIONSHIPS FOR D-C GENERATORS FOR USE IN AIRCRAFT, S. R. Bergman. AIEE TRANSACTIONS, volume 62, October 1943, October section, pages 613–16.

8. POTENTIAL BREAKDOWN OF SMALL GAPS UNDER SIMULATED HIGH-ALTITUDE CONDITIONS, M. J. Delerno. AIEE TRANSACTIONS, volume 63, 1944, March section, pages 109–12.

9. EFFECT OF ALTITUDE ON ELECTRIC BREAKDOWN AND FLASHOVER OF AIRCRAFT INSULATION, L. J. Berberich, G. L. Moses, A. M. Stiles, C. G. Veinott. AIEE TRANSACTIONS, volume 63, 1944, pages 345–54.

10. CABLE USED FOR TRANSMITTING ELECTRIC ENERGY IN AIRPLANES, Melville F. Peters, John J. Phillips, Max Kronstein, Helen B. Jealous. AIEE TRANSACTIONS, volume 63, 1944.

11. ALTERNATING VERSUS DIRECT CURRENT IN HIGH-ALTITUDE AIRCRAFT, R. W. Gemmel, J. C. Cunningham. *Aviation*, March 1944.



Figure 3. Directional stabilizer

gyroscope is employed as a reference for roll-axis and pitch-axis deviations (see Figure 2). Its outer case is mounted firmly to the structure of the airplane, but its rotor is universally mounted within the case so that the gyroscope's automatic erection system can maintain the rotor axis vertical, regardless of the position of the airplane. Any tilting of the gyroscope case with respect to the rotor is measured electrically by four potentiometers within the unit. These potentiometers have their windings fastened to the case and their sliders held in position by the gyro rotor. The resulting electrical signals activate the system to restore the aircraft to level flight. Since pitch-axis deviations are corrected by elevator control alone, only one potentiometer is located on the pitch axes. However, three potentiometers are located on the roll axis of the unit, as all three controls (aileron, rudder, and elevator) are employed to correct roll-axis deviations.

Directional or turn-axis control is derived from the directional stabilizer (Figure 3), which contains a horizontal gyroscope. The rotor of the horizontal gyroscope tends to remain fixed in space, unaffected by turning movements of the airplane. Here again, potentiometers with their windings fastened to the outer case and their sliders held in position by the gyro-rotor assembly are used to measure electrically any course deviation of the airplane. Two potentiometers are used, one for rudder control, the other for aileron control.

Thus, in the two gyro-controlled units a total of six potentiometers are employed as the sensing elements of the system. The individual units are identified in Figure 4 by the numbers 1 and 10, and the potentiometers by the numbers 5, 6, 11, 12, 13, and 14. A seventh potentiometer (number 2 in the illustration) is an auxiliary part of the Autopilot, used to energize a direction indicator (7) on the pilot's instrument panel.

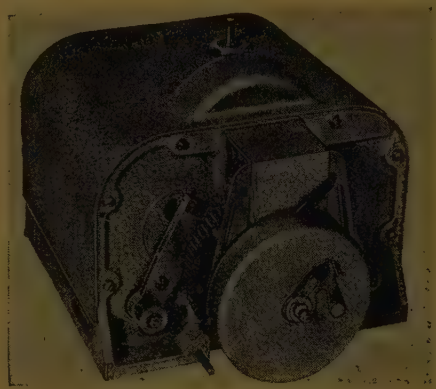


Figure 5. One of the three servo units

No less important in the operation of the Autopilot are the motorized servo units which do the actual work of moving the control surfaces (see Figure 5). Three such units are required, one each for ailerons, rudder, and elevator. (In Figure 4 these units are located at 15, 18, and 19.) Each servo unit contains an electric motor which drives a cable drum through a differential gear train. Two solenoid-operated clutches control the direction of cable-drum rotation; one clutch turns the drum in one direction, the other in the opposite direction. An automatic brake mechanism effectively holds the cable drum stationary when neither of the two clutch solenoids is energized. Cables wound on the servo-cable drum are attached to the main control cables which move the control surfaces.

Each servo unit also contains a balancing potentiometer with its winding fastened to the servo-unit frame and its slider attached to the cable drum. The purpose of these balancing potentiometers will be revealed later in the explanations of the controlling bridge circuits.

The seven-tube amplifier (Figure 6) is the electronic brain of the Autopilot. It contains three distinct channels, one for each of the three flight-control axes (item 16 of Figure 4). Each channel contains two tubes—a 7*F*7 double-amplifier stage, a 7*N*7 discriminator stage, and two relays which operate the clutch solenoids of the related servo unit. For the moment, each channel of the amplifier may be thought of as a motor controller which analyzes the amount and direction of control-surface movement called for by the sensing potentiometers in the bridge circuit and which then operates the corresponding servo unit accordingly.

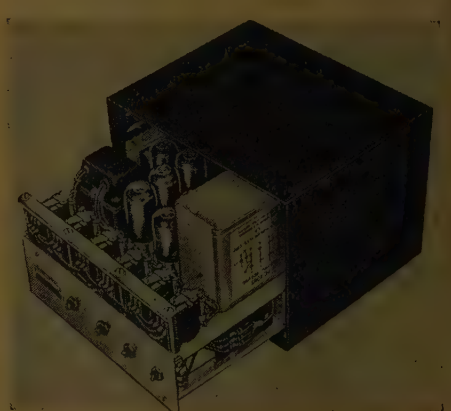
Manual control and adjustment of the system are provided by the Autopilot control panel (Figure 7). This unit centralizes at the pilot's fingertips the various switches and control knobs by which he engages the system, aligns the Autopilot with a selected attitude and heading, adjusts the sensitivity of the Autopilot, regulates the amount of control surface applied to correct a given deviation, and co-ordinates interrelated control functions. A "turn control" is included by which the human pilot can execute turns and evasive tactics without disengaging the Autopilot.

Power for the d-c components of the system is supplied by the airplane's batteries. A rotary inverter provides the

Figure 4. Schematic diagram of the Autopilot system



Figure 6. Autopilot amplifier



9-volt, 105-cycle a-c power required. The entire Autopilot equipment, with all seven motors, 14 solenoids, and seven-tube amplifiers energized and operating, draws only 7.6 amperes from the 26-volt a-c batteries. Normal voltage and frequency variations have relatively little effect on the performance of this Autopilot system.

The Bridge Circuits

If the gyroscopes are thought of as the *eyes* of the Autopilot, the servo units as the *muscles*, and the amplifier as the *brain*, then the bridge circuits might be likened to the nervous system by which all these components are interconnected.

There are three distinct bridge circuits of the resistance type in the system—one each for aileron, rudder, and elevator control. Each bridge circuit provides the input to a specific amplifier channel which, in turn, operates one of the servo units.

Figure 8 is a schematic diagram of the aileron bridge circuit, which has been simplified for the purpose of explanation. The aileron-control potentiometer, shown at the top of the diagram, is located in the vertical-flight gyroscope and is sensitive to roll-axis deviations of the airplane. The balancing potentiometer at the bottom of the diagram is actuated by the cable drum on the aileron servo unit. Both potentiometers are energized by a common transformer in the amplifier.

If we apply Kirchhoff's law to this circuit,

$$e_1 - e_2 = E \qquad (1)$$

As long as e_1 and e_2 are equal, E is zero and no signal is applied to the amplifier, and the servo unit remains stationary; but, when the control potentiometer is displaced, e_1 and e_2 are no longer equal and a signal E is impressed on the amplifier. Depending on the phase characteristic of E, the amplifier then causes the servo unit to drive the balancing potentiometer in the proper direction to make e_2 equal to e_1 and thereby reduce E to zero. In effect, the potentiometers, amplifier, and servo unit comprise a self-balancing bridge circuit in which the balancing potentiometer produces a corresponding movement of the cable drum to drive the control surfaces of the airplane. This is the fundamental operating principle of the *C*-1 Autopilot.

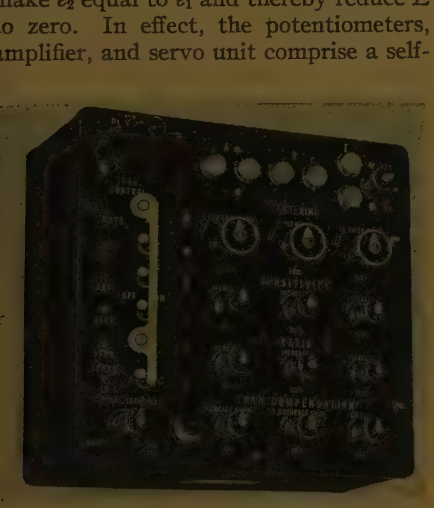


Figure 7. Autopilot control panel

Actually, of course, the circuit is complicated somewhat by the necessary inclusion of additional control potentiometers, as shown in the diagram of the aileron bridge circuit (Figure 9). The principle of operation, however, is the same; and the same type of equation applies:

$$E = e_1 \frac{r_1}{R_1} + e_2 \frac{r_2}{R_2} + (e_3 - e_4 - e_5) \qquad (2)$$

In this equation, E is the voltage output of the entire bridge circuit, and e_5 is the voltage applied by the balancing potentiometer.

When E is zero, the circuit is balanced

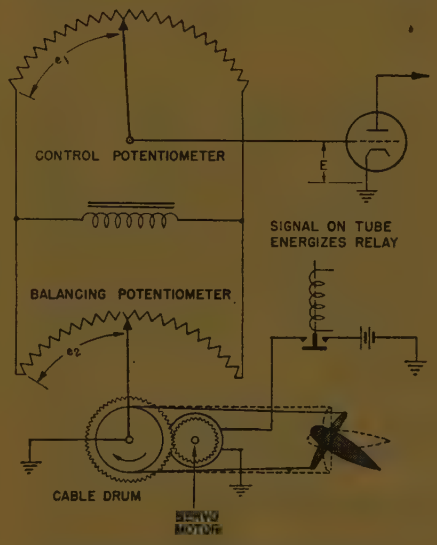


Figure 8. Simplified bridge circuit

and the system is at rest; but, whenever any potentiometer in the system is moved, the voltage (E) is effective on the amplifier and causes the servo unit to drive in the proper direction to adjust e_5 to such a value that E again will be zero.

In normal flight, e_1 and e_2 are zero, because the potentiometer wipers are at their center taps, as indicated by the dotted lines on the diagram. The other values are such that E is zero and the circuit is in balance. When the airplane is blown off course, the banking potentiometer on the directional gyroscope is displaced. This produces a signal, e_2, which causes operation of the servo unit and a corresponding increase in the value of e_5 to rebalance the circuit. Movement of the ailerons immediately causes a bank and consequently a reduction of e_3 as the vertical-flight gyroscope is tilted. This reduction of e_3 offsets the effect of e_2 and results in reversal of the servo unit to streamline the ailerons when the degree of bank becomes proportional to the directional deviation.

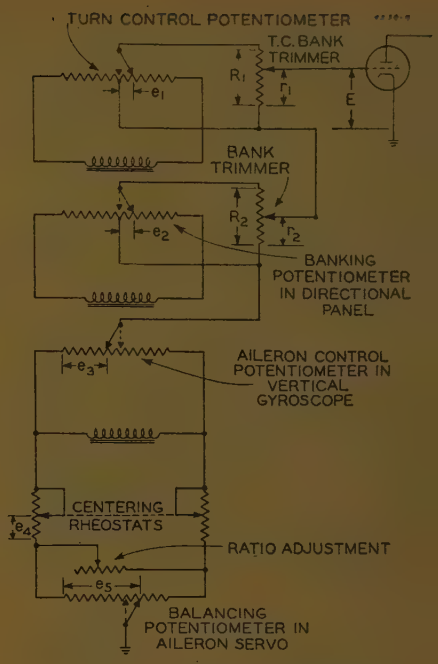


Figure 9. Complete bridge circuit

Meanwhile the airplane is turning back toward its original heading, reducing the value of e_2. Since e_2 is now less than the change of e_3, the circuit is again unbalanced, and the servo unit applies opposite aileron to reduce the degree of bank. As the airplane levels off, the value of e_3 increases toward its original level-flight value. Because both e_2 and e_3 now are approaching their normal values, the servo unit again reverses to restore e_5 to its normal value. Thus, the ailerons reach their trim position in level flight as the original heading of the airplane is restored.

Note that the turn-control trimmer and the bank trimmer are provided to regulate the effect of the turn-control and banking potentiometers by applying the fractional factors r_1/R_2 and r_2/R_2.

Among the advantages of using potentiometers in a-c bridges are:

1. Simple isolation of voltage sources.
2. Controllable amplification of small signals.
3. Linearity of control characteristics.
4. Nondistortion of wave forms.
5. Full 180-degree phase change at balance point.

A linear relationship exists between the deviation in attitude of the airplane, as measured by the potentiometers on the gyroscope and the displacement of the control surfaces. The voltage output of a bridge potentiometer can be considered as practically proportional to the displacement of the sliders, because of the high impedance of the amplifier input circuit when compared to that of the bridge circuit. The one exception to this linearity is the banking potentiometer on the directional gyroscope, which is represented by a single potentiometer

with linear characteristics in Figure 9, but which is actually a combination of two potentiometers. These two potentiometers are so arranged that their sliders are displaced in unison (see Figure 10), and the resulting voltage (e_2) is a parabolic function of the turn angle (θ). The voltage magnitude of this "dual-pot" slider displacement is given approximately by the formula

$$e_2 = a\theta^2 + b\theta \qquad (3)$$

in which a and b are constants dependent on the fixed resistances (r_3) and (r_4) in the bridge network. Equation 3 determines the magnitude of e_2 only for positive values of θ (deviation of airplane to the right). For determining the magnitude of e_2 when θ is negative (deviation to the left), the equation is

$$e_2 = a\theta^2 - b\theta$$

See Figure 15. Then if we substitute in equation 2,

$$E = e_1 \frac{r_1}{R_1} + (a\theta^2 + b\theta)\frac{r_2}{R_2} + (e_3 - e_4 - e_5) \qquad (4)$$

Employment of the resistance bridge with electronic motor control permits the substitution of simple and easily accessible electrical adjustments in place of more difficult mechanical adjustments. As an analogy, imagine how much easier it would be to adjust the carburetor, tappets, and distributor of your automobile if it could be done by turning knobs as you do in selecting a station on your radio. In the electronic Autopilot this ease of adjustment is attained simply by connecting rheostats (located on the control panel) to the bridge circuit as shown in Figure 9. The two centering rheostats are mounted on a common shaft connected in series with the balancing potentiometer, as shown. When the resistance of one is increased, the resistance of the other is decreased. This arrangement permits the pilot to shift the electrical center of the balancing potentiometer in either direction to align the circuit with the mechanical trim position of the control surfaces. These centering potentiometers thus can be likened to "electrical trim tabs."

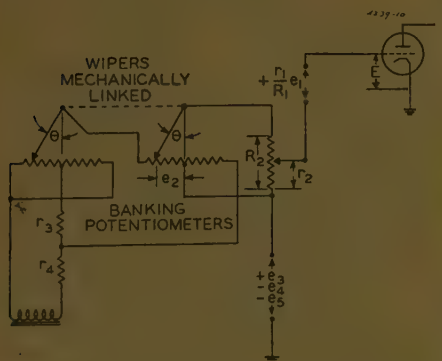


Figure 10. Partial circuit showing dual potentiometer arrangement

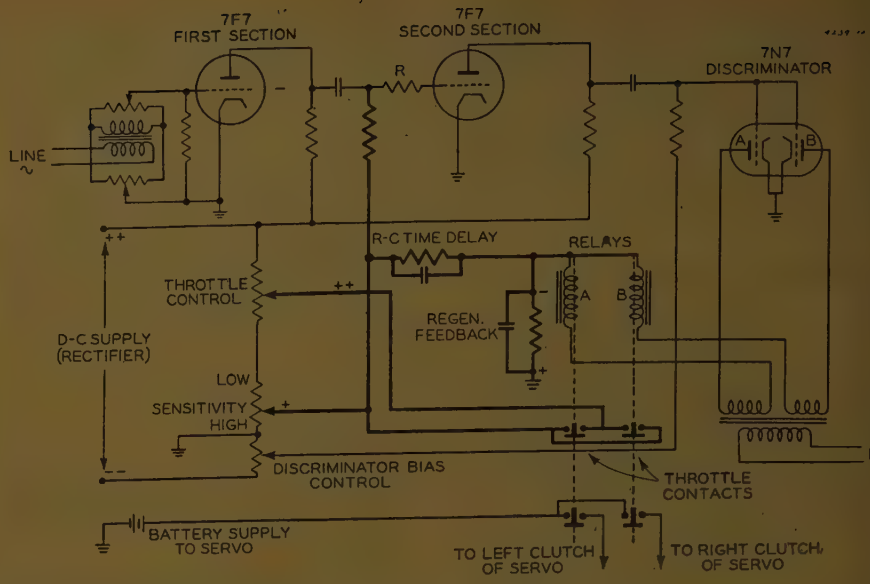


Figure 11. Schematic diagram of one amplifier channel

Another rheostat connected in parallel with the balancing potentiometer regulates the voltage drop across the potentiometer winding. This "ratio control" governs the amount of control-surface movement produced by a given displacement of the control potentiometer. This adjustment might be considered an electrical "gear shift," as it varies the airplane's "speed of response" for a given deviation.

With slight variations, the rudder and elevator bridge circuits are similar to the aileron circuit just described.

Amplifier Operation

So far, the amplifier has been described merely as a phase-sensitive motor controller which responds to any unbalance of the bridge circuits by closing the proper relay, thereby producing the required servo-unit operation. How the amplifier works and how it introduces special control characteristics are revealed by the schematic diagram of one of its three channels (see Figure 11).

Note that only two tubes are used in each channel, one 7*F*7 and one 7*N*7. The first section of the 7*F*7 tube is used strictly as an amplifier, with no d-c bias applied to the grid. The second section, however, is employed in a unique way as a control tube, with three separate controlling voltages applied to its grid.

Attention is called, first, to the resistance R which is connected in series with the grid of the second section. It is apparent that any voltage impressed on the left-hand end of this resistor is proportioned between the resistor and the internal grid resistance. When the applied voltage is negative, no current will flow between cathode and grid, as the grid resistance becomes practically infinite. Therefore, the entire applied voltage is impressed on the grid. However, when the applied voltage is positive, current flows between cathode and grid, because the grid resistance becomes very low in comparison with the resistance of R, and hence only a small fraction of the applied voltage actually is impressed on the grid. Thus, negative signal voltages applied to the resistor produce sharp plate-current fluctuations, whereas positive voltages do not cause any appreciable change in plate current (see Figure 12). In effect, this resistor lowers the saturation point of the tube's characteristic curve and produces a very flat curve beyond that point.

It is apparent from the resulting characteristic curve (Figure 12) that the *sensitivity* of the tube can be controlled by applying a variable positive bias. Thus, when the tube is biased at point A (high sensitivity) a small signal will produce

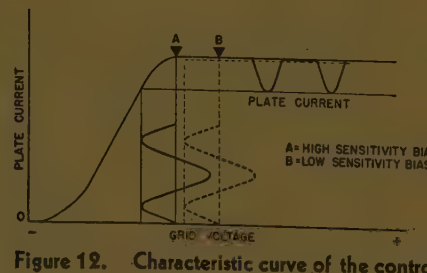


Figure 12. Characteristic curve of the control tube, illustrating the effect of changing sensitivity bias

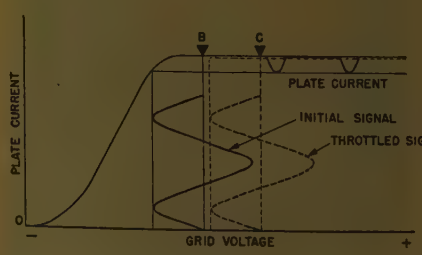


Figure 13. Characteristic curve of the control tube, illustrating the effect of throttling bias

plate-current variations. But when the tube is biased at point *B* (low sensitivity), the same signal is ineffective, and a larger signal is required to produce plate-current fluctuations. This sensitivity bias is knob-controlled by a potentiometer (Figure 11) located on the Autopilot control panel.

The sensitivity bias is applied continuously, but, under certain conditions of operation, two additional bias voltages are applied to this same tube section. One is a negative feed-back voltage which has the effect of increasing the tube's sensitivity whenever either half of the 7*N*7 discriminator tube is passing current. The other is a throttle voltage, of higher positive value than the sensitivity bias, which is applied to the tube whenever the throttle contact on either of the two relays is closed. The effect of this throttle voltage is to increase the bias on the grid to point *C* (Figure 13), thus blocking any signals which are not sufficiently large to overcome this increased bias.

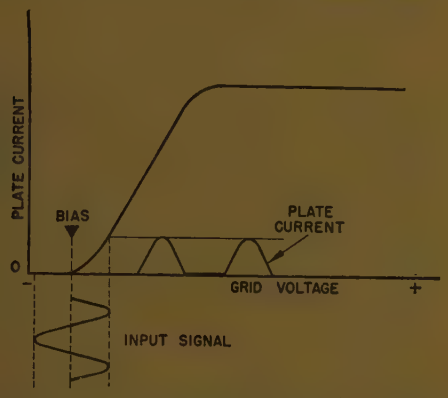


Figure 14. Characteristic curve of the discriminator tube

(Referring to Figure 13, note that a signal which produces plate-current fluctuations when the tube is biased at *B* is ineffective when the bias is changed to *C*.) A resistance–capacitance time-delay circuit serves to maintain the throttle bias on the tube for a short time after the relay contacts reopen.

The purpose of the feed-back circuit is to insure that the weakest signal capable of overriding the bias of the control tube will be amplified sufficiently to drive the discriminator tube, and thus produce positive nonchattering operation of the relay.

The discriminator tube, which employs alternating voltage on the plates, is biased negatively to the cutoff point. This bias is sufficiently large to prevent any current flow through either half of the tube when no signal is applied to the grid (see Figure 14).

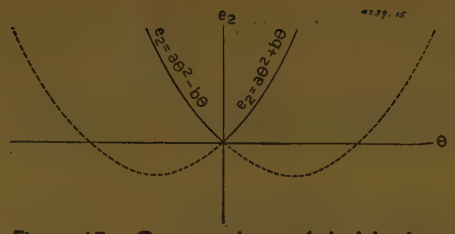


Figure 15. Output voltage of dual banking potentiometers versus directional deviation of airplane

It is apparent from the diagram (Figure 11) that since the two plates of the discriminator tube are connected to opposite ends of similar transformer windings, the voltage of one plate is always 180 degrees out of phase with the voltage of the other plate. When the signal applied to the interconnected grids is positive, the electrons will flow from the cathode to whichever plate is positive at the same instant. Since each plate circuit contains a relay coil, the corresponding relay is energized. Thus the discriminator tube operates one relay or the other, depending on the phase relationship between the input signal and the alternating plate voltage supply. In effect, this discriminator is a grid-controlled rectifier similar to a thyratron.

As illustrated by the diagram, individual relays control the operation of separate clutches in the servo unit and thus control the direction and amount of servo-unit rotation or control-surface movement.

Now, let us follow a signal from the bridge circuit all the way through the amplifier circuit. This signal will be one which calls for counterclockwise rotation of the servo unit, as would be produced by energizing the left-hand clutch solenoid. That is, the signal is in phase with the voltage at plate *A* of the discriminator tube. The signal is first amplified and reversed in phase by the first section of the 7*F*7; then it is applied to the grid of the second section where it produces plate-current pulsations during the negative half of the cycle. Since the signal phase again is reversed in the second section, these plate-current pulsations apply a positive half-wave signal to the discriminator tube which is in phase with plate *A* of that tube. The resulting current flow energizes relay *A*, which in turn energizes the left-hand clutch solenoid of the servo unit.

At first the signal on the control-stage grid of the 7*F*7 is sufficiently large to override the additional positive bias introduced by the closure of the throttle contacts on the relay. However, as the servo unit drives the balancing potentiometer toward the balance point, the signal gradually is reduced until its negative peaks no longer exceed the throttle bias. When this occurs, the signal to the discriminator tube is interrupted, causing the relay to drop out for about one-eighth second while the time-delay capacitor discharges.

When the capacitor is discharged, the throttle bias is lost and the signal to the discriminator is restored. The relay closes again, but only momentarily, because the throttle bias is thereby reapplied. The result is that, as the servo unit approaches the balance point, it operates intermittently with a "pecking" action which prevents overshooting or "overcontrol" of the airplane. The rate of correction, which is fast for large deviations, will diminish gradually as the airplane approaches a stabilized-flight attitude.

Summary

In automatic flight control, as in other control problems, the limit of flight accuracy through mechanical and electrical controlling methods seems to have been reached. Only by the application of electronic-control principles has it been possible to set new standards and to achieve full flexibility of control with completely automatic operation of all control functions. Furthermore, these advantages have been obtained with actual reductions in the total weight of equipment.

Since the use of an automatic pilot for precision bombing imposes complex and rigid requirements which are not encountered in commercial aeronautics, it can be confidently stated that the continued development of electronic controls will produce a small lightweight, foolproof automatic pilot for peacetime use. Because of the precision bombardment Autopilot, the control industry is certainly richer in knowledge and experience, and will be better able to cope with the new problems of peacetime industry.

Although the connection now seems obscure, some of the applications in the development of the type *C*-1 Autopilot were taken from a cabin-temperature-control system. In a similar way, the principles employed in both systems have been found useful in the automatic regulation of engine power. And so, in a seemingly endless chain, each new development brings to light the solution of another problem. Who knows what application these same developments will find in enlarging the prosperity, health, and happiness of a peaceful world?

Paralleling and Regulation of 24–28-Volt D-C Generators in Multiengine Aircraft

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Synopsis: This paper presents the findings of a rather extensive program of testing electric systems in aircraft. Only the system which is presently being used in most United States Army Air Forces multiengine aircraft and a proposed system, which has been designed as the result of the tests, are described. All intermediate steps are not discussed since it is felt that the effects of these steps have been reflected in the proposed system. The proposed system differs from the present system primarily in that it employs separate networks for power feed and distribution instead of a common bus. Several desirable features of such a system are pointed out. Equipment is discussed in a general manner since the aircraft designer is concerned with the design of equipment only insofar as it affects the operation of the system. However, quite often the aircraft designer is in a position to make suggestions for improvement of the equipment.

MOST large United States Army Air Forces aircraft of the present day employ the type of electric system that is outlined by the Army Air Forces specification 32300, "Installation of Electrical Equipment, General Specification for." This is a 24–28-volt d-c single-wire grounded negative system in which the nominal rating of the batteries is 24 volts and the engine-driven generators deliver power at 28 volts. All power is fed into and taken from a common bus which interconnects all wiring of the airplane as shown in Figure 1.

Four main-engine-driven generators and one or more batteries supply power to the airplane. An auxiliary power plant, gasoline-engine driven, is available for ground operation, take-off, and landing. A bus exists across the airplane from number 1 to number 4 nacelles and from the nose of the airplane to the tail. Each generator feeds into the bus at its respective nacelle through a reverse-current relay. The batteries feed into the bus through a relay which is operated by the pilot. External power may be supplied to the airplane while on the ground through an external power plug. It can be seen from this that normally there are five sources of power feeding into the bus: the four generators and the batteries. It is evident that electrical resistance must exist between each and every feeder point. With current flowing the voltage drop, or differential voltage, between inboard and outboard nacelles may be as much as one-quarter volt. To overcome this differential, the outboard generators must generate a higher voltage than the inboards if equal load distribution is to be expected.

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Generator Relay

The generator is connected to the bus by a relay (see Figure 2) which closes only when the generator is delivering voltage higher than a predetermined value (approximately 26 volts). A small "pilot" relay *V* which determines the "cut-in" voltage operates a set of contacts *T* in the main winding. The generator switch which is controlled by the pilot completes the circuit. In addition, the relay opens in the event that generator voltage drops below bus voltage and causes reverse current to flow through the generator. Permanent magnets are used to polarize the pilot relay so that current passing through coil *R* in the normal direction strengthens the field but reverse current induces magnetomotive force in opposition. As the induced magnetomotive force approaches the strength of the permanent field the attraction on the relay armature is nullified and the contacts open. It is this function that gives the relay its common designation as a reverse-current relay.

A new relay has recently been developed to replace the reverse-current relay. It is referred to as a differential-voltage relay and gets its name from its operating characteristic. Provided the generator is delivering voltage higher than the bus voltage by a minimum value (between 0.25 and 0.50 volt) the relay will close and put the generator in service. In this case, the "pilot" relay operates on the differential in voltage between the generator and bus. There is a series current coil, similar to that in the reverse-current relay, which performs the same functions. These are:

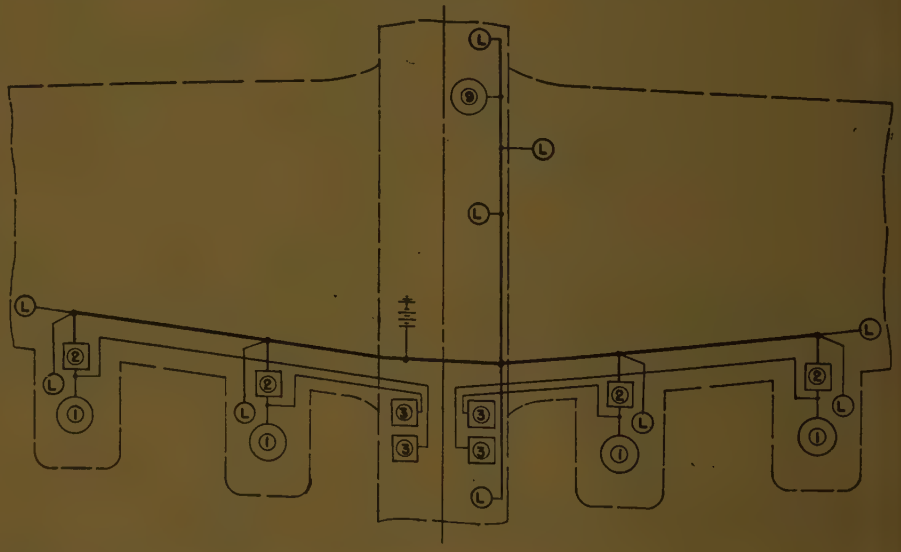
1. To keep the pilot contacts closed after the main relay closes, at which time the differential between generated and bus voltage no longer exists.
2. To open the relay when a reverse current of sufficient magnitude flows into the generator.

Voltage Regulation

Circuit details for one generator are shown in Figure 3. System voltage is regulated by individual voltage regulators (Figure 2) for each generator. Regulation is accomplished by varying the resistance of the generator shunt-field circuit so that a constant voltage is maintained within the range of operating speeds regardless of load conditions. This variation of field resistance is accomplished in the regulator, by the influence of the voltage winding *N* upon the variable resistor *L*. The generator field current is taken through the resistor; variation in resistance causes varia-

Figure 1. Schematic wiring diagram, typical four-engine airplane

1—Engine-driven generator
2—Reverse-current relay
3—Voltage regulator
9—Auxiliary power plant
L—Load center



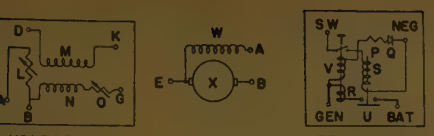


Figure 2. Schematic wiring diagrams, typical equipment

A—Generator field terminal
B—Generator positive terminal
D—Equalizing-coil terminal
E—Generator negative terminal
G—Ground (negative) terminal
K—Equalizing-coil terminal
L—Variable resistor (field)
M—Equalizing winding
N—Voltage winding
O—Variable resistor (adjusting)
P—Fixed resistor (current-limiting)
Q—Rectifier
R—Series current coil
S—Main relay winding
T—Pilot relay contacts
U—Main relay contacts
V—Pilot relay winding
W—Generator field windings
X—Generator armature
BAT—Bus connecting terminal
SW—Switch terminal
NEG—Negative terminal
GEN—Generator positive connecting terminal

tion in generator field current, thus maintaining a constant voltage under all conditions of speed and load within rated limits.

The variable resistance *O* is connected in series with the solenoid regulating coil so that a greater or lesser portion of the line voltage is imposed on the regulating coil, thus increasing or lowering the regulating voltage. Normally, this is the only adjustment made on the regulator.

The regulator is connected to the individual system at the generator side of the reverse-current relay, and is centrally located in the fuselage of the airplane for accessibility in flight. This results in another voltage differential in the system, in addition to the voltage drops in the busses because the connection between the reverse-current relay and the regulator is a different length for each generator. In considering the functions of this connection it is necessary to analyze the currents which it must carry. They are:

1. The current necessary to energize the regulator.
2. Field current for the generator.
3. Operating current for the voltmeter.
4. Operating current for main winding of the reverse-current relay.

It follows that a connector intended to energize the regulator is performing three additional functions. All of these currents are reasonably constant except the generator field current which may vary from approximately three to ten amperes. As a basis for comparison it may be assumed that the length of this connector is approximately 30 feet from the outboard nacelle to the regulator and 20 feet from the inboard nacelle to the regulator. A simple calculation will show that under identical conditions the voltage drop across this connector would be 50 per cent higher for the outboard nacelle than for the inboard. The effect of this condition upon the generator, as shown in Figure 4, is that the generated voltage exceeds the regulator setting by a value equal to the drop across the line. In some cases this value may be as high as one volt. This analysis shows that under all conditions the outboard generators will be delivering a different voltage than the inboards, if all regulators are adjusted to the same value. Hence, a natural tendency for poor load division is introduced.

Load Division

Voltage regulators are provided with an equalizing circuit, which tends to make all generators deliver equal current. The equalizing circuit provides for equal distribution through the influence of winding *M* on winding *N* and the resultant effect on resistor *L*, as shown in Figure 2, by lowering the voltage of the generators which are tending to deliver high current and conversely raising the voltage of those generators which are delivering comparatively low current. In this respect, if one generator is out of service and the equalizing circuit is left intact, it will endeavor to lower the voltage of the others. At the same time the regulator will tend to raise the voltage of the "off" generator so that it will take more load. Since the one generator is inoperative, as far as the system is concerned, its voltage is zero. It is impossible to lower the voltage of the other machines to such a value. However, the equalizing circuit will exert its full energy to accomplish this. The result is a drop in voltage of the generators in service. The magnitude of this voltage drop is dependent upon the number of generators in the system, the generators out of the service, and the total load on the system. At any time a generator is out of service the respective reverse-current relay is open whether the pilot's control switch indicates "on" or "off." For instance, if the switch is "on" but the generator speed drops to a point where it can not carry the load its reverse-current relay will open and remain open until the speed rises again. Thus, it becomes desirable to devise some method whereby the equalizing circuit will be opened automatically. The simplest method for accomplishing this is to add auxiliary contacts to the reverse-current relay. This would require practically no change in the airplane wiring since the equalizing connection from the generator to the regulator usually passes close to the relay.

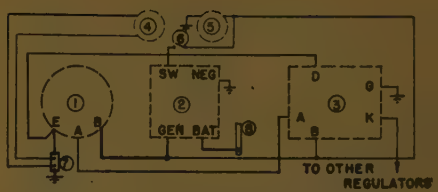


Figure 3. Schematic wiring diagram, one generator circuit

1—Generator
2—Reverse-current relay
3—Voltage regulator
4—Ammeter
5—Voltmeter
6—Generator switch (control)
7—Ammeter shunt
8—Positive bus

Another solution to the problem employs a double-pole single-throw toggle switch rather than a single-pole single-throw as the generator switch. The extra pole is used to open the equalizing circuit at the same time the generator switch is opened. This accomplishes the desired results provided the generator is intentionally put out of service or the operator knows the generator is inoperative, and opens the switch. The objections to this solution are that it is not automatic and additional resistance is introduced by the additional wiring which is required to extend the circuit to the point at which the switch is located.

Energy for the equalizing circuit is obtained from a voltage drop across a portion of the generator circuit, which is specifically the connection between airplane structure (ground) and the *E* (negative) terminal of the generator, as shown in Figure 3. In accordance with Air Corps Specification 32,300 the resistance

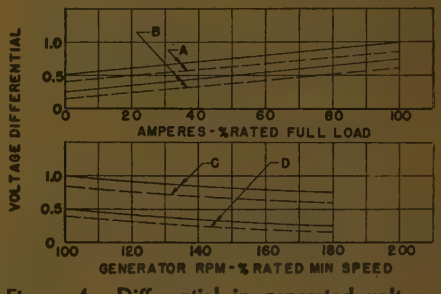


Figure 4. Differential in generated voltage and regulator setting due to resistance of connections between the generator and voltage regulator for a typical four-engine aircraft

Solid curves—Generator on outboard engine
Dashed curves—Generator on inboard engine
A—Generator operating at rated minimum speed
B—Generator operating at rated maximum speed
C—Generator delivering rated full-load current
D—Generator operating at no load

of this portion of the circuit is to be maintained at a value which will give a drop of one-half volt ±5 per cent with rated current flowing. However, for any given airplane a deviation between generators of more than two per cent should not be exceeded. On some airplanes this drop is accomplished through the use of a given length of copper conductor, the resistance of which may vary widely with temperature. Consequently, the voltage drop may vary with ambient temperature and load conditions. In some airplanes this condition is accentuated by the ground circuit for the inboard generators being located in the wheel well. The conductors which comprise the circuit are exposed to outside air temperatures, which may differ by as much as 200 degrees Fahrenheit from the ambient temperature in the outboard nacelles. The ammeter shunt is usually located in this circuit and with the necessary connectors contributes to the required resistance. A loose connection anywhere in the circuit may cause erratic results as to the voltage drop under given conditions, since contact resistance varies widely when passing current. In this type of installation, all such connections must be controlled. Furthermore, it is important that the ground connection be made to basic structure. In some cases this connection is made to anodized parts. The resistance of an anodized connection is as unpredictable as a loose connection. The equalizing circuit has a low resistance and operates on low voltages. Since the total voltage is one-half volt across the ground circuit at full load and the equalizing circuit is to operate not on this entire value but on differences existing between generators, if a two per cent tolerance is to be maintained in load distribution, the equalizing circuit must be capable of operating on voltage differentials down to 0.01 volt, the drop resulting from a flow of two per cent rated current. The resistance of the winding in the regulator itself is approximately 0.4 ohm. For this reason it is important that the connection between the generator ground circuit and equalizer circuit of the regulator have a low resistance. Assuming that the lead resistance is no more than 0.1 ohm, the total resistance of the circuit for one generator is 0.5 ohm. With 0.01 volt across this value of resistance the current flow would be in the order of 20 milliamperes.

To minimize the effect of resistance changes of the generator ground circuit due to temperature it is desirable to use a material having a lower thermal coefficient of resistivity and a higher resistivity than copper. Such a material would result in a more constant resistance under all temperature conditions and a probable reduction in weight. Manganin, which is widely used in instrument shunts, is the most logical choice for this purpose. Manganin is at present a critical material. However, even during normal conditions its cost would probably prohibit its use. Nichrome was found to be a suitable substitute. Based on the use of 200-ampere generators, a resistor necessary to provide a one-half volt drop weighs only 10 ounces compared to approximately 3.5 pounds of wire used for the same resistance. Another advantage of having such a unit rather than a length of wire is that it can be calibrated before installation to a close tolerance. When a resistor of this type is used it is advisable to make the equalizer-circuit connection at the resistor rather than at the generator. This makes the connection across only a calibrated unit and eliminates the effect of the necessary connections in the circuit. The importance of this can not be stressed too highly since successful parallel operation depends primarily on the equality of the ground-circuit resistance.

From the standpoint of weight saving and wiring simplification, it is desirable to incorporate the ammeter shunt with the resistance unit. It is a simple matter to tap off a portion of the resistor which will give the same voltage drop as the ammeter shunt now being used. This value is usually 50 millivolts at 300 amperes. The addition of such a tap increases the weight of the resistor unit by a negligible amount, whereas the ammeter shunt weighs approximately 12 ounces. This eliminates an extra part as well as several connections.

Study of the foregoing shows that there are four major points to consider which affect load distribution between generators. They are:

1. The location of the reverse-current relay with respect to the system.
2. The length and size of connection between the reverse-current relay and the regulator.
3. The resistance value and equality of the generator ground circuits.
4. The total resistance and equality of the regulator equalizing circuits.

The power in such a system is drawn from the bus at various points; that is, at each nacelle and several points within the body of the airplane. There is no central power distribution point.

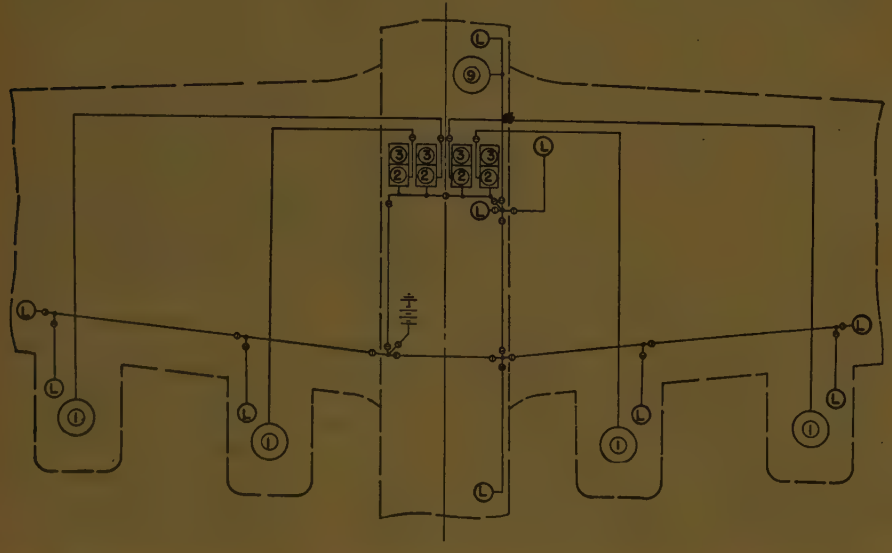
Proposed System

A desirable aircraft electric system would utilize all of the previously described equipment and eliminate the undesirable characteristics of the present system. As the result of tests conducted on the component parts and the system as a whole in the laboratory and on airplanes, a revised system,* shown in Figure 5, was proposed. The proposed system consists primarily of two networks, one a feeder network and the other a distribution network. All power is fed into and taken from a single point which is common to the two. The output of each generator is fed directly into the body of the airplane to a central point, where its reverse-current relay and voltage regulator are located, mounted adjacent on a panel. There is an individual panel for each generator. The output sides of the relays are connected by bus to the central power point. This constitutes the feeder network. A radial network distributes power to various load centers in the airplane.

Analysis of this system shows that regulator connectors of excessive and unequal lengths are eliminated. Line re-

Figure 5. Schematic wiring diagram, proposed system

1—Engine-driven generator
2—Reverse-current relay
3—Voltage regulator
9—Auxiliary power plant
L—Load center
⊖—Location for suitable circuit protector


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The Gyrosyn Compass

O. E. ESVAL
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IN all military and commercial aircraft, the gyroscope is depended on for attitude and direction references. The vertical spinning gyroscope provides attitude indication in the form of the artificial horizon. At present, other attitude representations based on the vertical gyroscope are available. The use of the vertical gyroscope has been accepted for 15 years or more, and will be further improved for indication of roll and pitch but probably never superseded. The indication of azimuth, or heading of the airplane, which is actually the attitude around the vertical axis called direction, up to the present has had no standard means of presentation. In the past, magnetic compasses have been used for this purpose. These instruments for aircraft use are imperfect, and it has been found necessary to supplement this type of indication with a secondary reference in the form of a directional gyroscope. With the addition of the directional gyroscope, the inherent defects of the compass are greatly minimized. These defects are well known and can be stated as

1. Lack of stability.
2. Northerly turning error.

The directional gyroscope is essentially a gyroscope with the spin axis horizontal and mounted in gimbals with freedom about the vertical such that it tends to stay fixed in space around this vertical axis. A card fixed or geared to the vertical-axis gimbal will show changes in heading and for appreciable periods of time will provide a reference for maintaining a straight course. It is, however, subject to slow wander, and therefore must be corrected once in a while by manual resetting. This is done holding flight on a straight and level course. Then the conventional magnetic compass gives fairly reliable information and the directional gyroscope can be set to agree with its average reading. It is obvious that this system of periodically resetting the directional gyroscope to compensate for its random drift is a serious handicap to instrument flying. Also, in military aircraft, the usefulness of the magnetic compass in the cockpit is diminished because of the disturbances on the magnetic field due to the proximity of electrical components and armor plate.

Statement of Problem

The purpose of this paper is to describe, in a general manner, the development and characteristics of a system which largely eliminates these serious shortcomings. The first approach to a problem of this kind is to establish the basic requirements of a satisfactory direction-indicating system. The requirements are as follows:

1. The indication shall be stable in all conditions of air turbulence.
2. No manual resetting or attention on the part of the pilot shall be required.
3. The readings must be free from deviation and deviation changes.
4. Several repeaters of the indication must be provided for, each giving a stable or deadbeat indication.
5. The equipment must be simple, both mechanically and electrically, and all parts requiring periodic service must be easily accessible.
6. The accuracy must be commensurate with the limitations of the earth's magnetic field.
7. Turn errors must be negligible.
8. The entire system shall have minimum weight and volume.

In attacking this problem, several points stand out. It becomes apparent that it is necessary to operate from the earth's magnetic field at some part of the aircraft remote from magnetic disturbances and to transmit the indication to various points convenient to the flight personnel. The location for the magnetic sensitive unit should be in a part of the airplane where deviation and deviation changes are zero, or a minimum. Therefore, it must be away from guns, turrets, cargo, motors, and so forth. Such a location may, for example, be the very tip of a wing, the top of the vertical fin, or some part of the stabilizer. To enable mounting in such confined spaces, the unit itself should have the smallest possible physical dimensions. It also should be simple and rugged, to withstand the vibration and buffeting that is often severe in remote parts of the airplane. In addition, since such locations prob-

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istance between relays is eliminated. All equalizing circuits have equal resistance. In such a system the disadvantages of the present system are overcome. In addition, a reduction in the weight of the electric system seems possible.

In any electrical service circuit protection against fault damage is usually employed. Circuit protection is particularly desirable in military aircraft because of possible damage from enemy gunfire. Parallel wiring has been advanced as a possible method of protection against failure of electric equipment due to damage of wiring from gunfire. Combat experience has indicated that such damage is not frequent. However, if the added precaution is desired parallel wiring can be applied to any system. Parallel wiring is a modification of any given system and should not be considered an additional system.

Locations for suitable circuit protectors are shown on Figure 5. The term "suitable protector" is used because no one type of protector is capable of performing the desired functions in every location. Various sizes and types will be required. Comparison of Figures 1 and 5 will show the difficulty in attempting comparable protection with the present system.

A comparison of the two systems shows that the desirable features of the proposed system are as follows:

1. There is a central distribution point from which all power is taken.
2. All power is fed into and regulated at this point.
3. Better load distribution between the generators is obtained because all generators are electrically symmetrical about the load center and do not have inherent circuit inequalities which the regulator must overcome. The only function the equalizing circuit of the voltage regulator is required to perform is that of compensating for irregularities in the voltage regulation.
4. Relays and regulators are accessible in flight for adjustment and replacement, if necessary.
5. Since each individual circuit on the airplane carries primarily its own load current, it is possible to provide adequate circuit protection for a given circuit without undue influence on other circuits.
6. Wire sizes of the power distribution lines can be reduced to take advantage of the central supply point to the extent that the effective resistance between the power supply and the load is decreased. This allows a smaller wire to be used in many cases where wire sizes are controlled by resistance. In many cases it is possible to rate the wire thermally allowing a reduction in wire weight. A comparison of the two systems as used on a typical four-engine aircraft shows that a weight reduction of approximately 38 pounds is effected by replacing connectors with smaller wire sizes wherever possible.

Observations on test airplanes employing the proposed system have shown that voltage regulation and power distribution are improved. Also, maintenance time required for the electric system has been decreased.

ably are somewhat inaccessible, the device must be particularly durable to eliminate frequent servicing of that part. All other parts subject to periodic servicing must be in the cockpit or other easily accessible location.

Gyrosyn Compass

The Gyrosyn compass provides one method of meeting the indicated requirements. This system consists essentially in automatically and continuously correcting the directional gyroscope for its random drift by indications derived from a direction-sensitive device located in a magnetically undisturbed region of the airplane. The need for manually resetting the directional gyroscope is eliminated, and it thereby serves also as a space-stabilized compass. The direction-sensitive device is termed a flux valve. It may be located in practically any part of the airplane because it is small. In this system, the directional gyroscope is synchronized by the flux valve to magnetic meridian. This explains why the name "Gyrosyn" was selected.

Vertical Gyroscope Analogy

It is interesting to note that this treatment of the directional gyroscope for direction indication is an exact analogy to the conventional use of the vertical gyroscope in which oscillations of short-period pendulums are integrated to establish accurate attitude indications without recourse to manual resetting. Why the directional gyroscope remained a "free-gyro" device for so many years after the vertical gyroscope was made to be controlled deserves explanation. First, simple devices such as the flux valve were unobtainable at the time the first gyro vertical was constrained to seek the vertical of its own accord, and also old-style magnetic compasses were fairly adequate until flying conditions became too exacting. Actually, controlled directional gyroscopes have been in use for a number of years, mainly in Europe. These, however, do not provide the simplicity of a short-period pendulum, are expensive to build, and while providing stable indications, fail to meet other requirements set forth above.

Description

A representative installation of the Gyrosyn compass is shown in Figure 1. It is noted that an alternate position of a flux valve is indicated. The system comprises two main units shown in Figure 2, Gyrosyn indicator (with amplifier), flux valve with connecting cable and optional repeater unit; Figure 3 shows the flux valve (close-up). The primary indication in this system is the directional gyroscope, and it is shown in the cockpit for use by the pilot as a flight indicator. This is the normal application. There are cases, however, in which the primary indicator may be allotted to some other member of the crew, such as the navigator. The sensitive pickup device embodying the flux-valve elements is shown in Figure 4. It is essentially a permeable-core device having an exciting winding and three phases spaced 120 degrees with signal pickup coils on each. A voltage is generated in the respective coils, depending on the relative position in the earth's field. By virtue of the fact that the output voltage changes sinusoidally with the position of each phase in the earth's field, it is possible to indicate the direction of the earth's magnetic field relative to the flux-valve element at a distant point. This is done by connecting the coils to respective phases of a self-synchronous unit located at primary indication location. Without going deeply into the theory of operation, it suffices to say that the induced voltage of the rotatable secondary winding of the self-synchronous unit has a magnitude and polarity determined by its angular displacement relative to the angular displacement between flux-valve element and the earth's magnetic field. By properly positioning the secondary winding, or rotor of the self-synchronous unit, angularly, a null signal may be obtained. This is the normal self-synchronous position. The rotor of the self-synchronous unit is linked mechanically to the vertical ring of the directional gyroscope. The electrical and mechanical schematic diagram of this system is shown in Figure 5. Shown also on this diagram is a precessing device which applies torque around the horizontal axis normal to the spin axis of the gyroscope. The precessional torque is controlled in magnitude and direction by the polarity and strength of the signal from the rotor of the self-synchronous unit, after amplification and rectification. The torque applied about the horizontal axis of the gyroscope simply produces rotation around its vertical axis, thereby driving the self-synchronous-unit rotor to a position where the signal becomes zero. In other words, the gyroscope is constrained to the position that maintains null self-synchronous-unit output. This means that the spin axis of the gyroscope will remain oriented in a fixed position relative to the earth's magnetic field, or magnetic meridian. The directional gyroscope, therefore, seeks the magnetic meridian in a manner very similar to the way in which a vertical gyroscope seeks the vertical. The only conditions under which the output of the self-synchronous unit is not null occurs when the gyroscope in its normal manner slowly drifts away from its correct azimuth position, or when the system is "turned on," preparatory to making a flight, that is, the period of

Figure 1. Airplane with Gyrosyn compass

Gyrosyn-compass system
C-1—Turn-indicator adaptation
Total weight with one flux valve and two repeaters $11\frac{1}{2}$ pounds estimated
A—Flux valve and mounting bracket—weight 1 pound actual
B—Gyrosyn-compass indicator—weight 8 pounds estimated
C—Gyrosyn-compass repeater—weight $1\frac{1}{4}$ pounds actual
1—Gyrosyn-compass indicator
2—Repeater
3—Flux valve
4—3 phase, 400 cycles, 115 volts, 27 volts direct current
5—Indicating pointer
6—Course setter
7—Cover

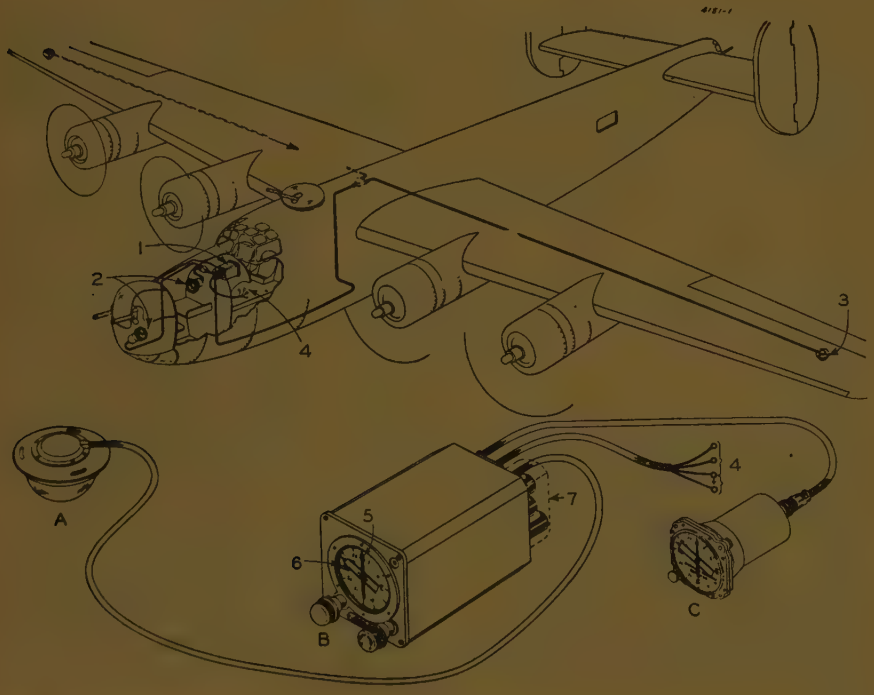




Figure 2. Gyrosyn-compass indicator (with amplifier), flux valve with connecting cable, and optional repeater unit

initial synchronizing. Heading changes are made without precessing the gyroscope and without losing synchronization. This is due to the fact that the magnetic meridian with respect to the flux valve and the directional gyroscope and self-synchronous unit with respect to the aircraft move through exactly equal angles, and therefore the null is not disturbed. The acceleration experienced in turn will temporarily disturb the null, but this will not produce a serious error, as described later. It is shown in Figure 5 that the flux valve in itself is a pendulum with two degrees of freedom. It is suspended on a universal joint in a manner normally to keep the plane of the flux valve horizontal. This must be done so that the indication obtained from this element represents the direction of only the horizontal component of the earth's magnetic field. Thus, a change in attitude of the airplane in roll or pitch cannot tilt the flux valve. It will remain in the horizontal plane. Certain disturbances, however, momentarily may swing it out of the horizontal. Such disturbances will make the signal fluctuate and give momentary erroneous data. In rough air, the signal may almost continually fluctuate. This brings us to the important function of the directional gyroscope as a primary indicator. It serves effectively to integrate all short-period disturbances and oscillations. The maximum precession rate as called for by the flux valve through the self-synchronous unit is limited. The period of pendulum, on the other hand, is so short that a single oscillation of the pendulum cannot be detected by observation of the gyroscope. The result in the Gyrosyn is that this system provides deadbeat accurate indications that are unchanged by any weather conditions.

Turning Error

So far, it has been shown that most of the requirements stated in the beginning can be met by a simple addition to the directional gyroscope, and by two extra elements, the flux valve and the amplifier. A deadbeat remote-indicating magnetic compass is obtained. Another of the requirements stated is the reduction of the northerly turning error. This error does exist in the Gyrosyn compass as in any magnetic-compass system that directly or indirectly is controlled by gravity. If the craft is turning, the resulting acceleration acts on the pendulum to displace it from a horizontal position. This results in an erroneous signal, the amount and direction of which depends on the instantaneous heading of the aircraft during the turn. The gyroscope, therefore, commences to move away from the magnetic meridian, but it does not do so rapidly. As a matter of fact, for a normal



Figure 3. Flux valve (close-up)

turn the duration is not sufficiently long to cause the gyroscope to be in error more than a few degrees, usually not more than five degrees. If the turn continues indefinitely, or, in other words, the airplane continues to circle, the error signal reverses on itself and does not cause accumulation of gyro errors. Rather, the gyro indication will slowly move back and forth about its correct position. It should be noted that definite limits are placed on the gyro precession rate. The precession characteristics against angle of error is nonlinear. This being the case, sustained oscillation of the pendulum resulting from air turbulence or poor manual flying, may cause desensitization of the coupling between flux valve and gyroscope. To overcome the oscillation, a small amount of damping is introduced on the flux-valve pendulum. It is accomplished by enclosing the element in a sealed chamber and filling with damping fluid. The damping coefficient may vary through wide limits without impairing the accuracy of the Gyrosyn compass, and therefore wide variations in temperature encountered do not, by changing the damping-fluid viscosity, affect the system operation.

Alternate Turn-Error Corrections

In applications where the gyroscope is used as an automatic-pilot reference for rudder control, another means of limiting turn errors is usually available. Turns with an automatic pilot are initiated by a co-ordinated control of some sort. This control can be used to suspend the flux-valve signal for the duration of the turn. The gyroscope therefore becomes a free directional gyroscope with no constraint and will accumulate for the duration of the turn only a very negligible error because of its random wandering. At the resumption of straight and level flight, flux-valve control is automatically applied, and the gyroscope is again under the gentle influence of the flux valve. The same effect may be accomplished in a Gyrosyn system, independent of the automatic pilot by having the flux-valve signal interrupted by a rate-of-turn relay, such as a rate gyroscope. For most practical cases, however, the small temporary error induced with flux-valve control functioning during the turn is of no importance.

Accuracy

In stating the accuracy of the Gyrosyn compass as in any other type of remote-indicating magnetic compass, the conditions first must be fully defined. With variation of plus or minus 40 cycles in supply frequency and a corresponding change in voltage, both alternating and direct, and field strength change from 0.04 to 0.40 oersted, and the maximum error of definition is plus or minus $1^1/_2$



Figure 4. Flux-valve element

degrees from the magnetic meridian to the gyro indicator. Higher accuracy could be obtained but is hardly justified in view of the limitations of the earth's magnetic field. However, when electric generators and power supplies are improved to be more constant in voltage, frequency, wave form, and so forth, these performance figures will automatically be further improved.

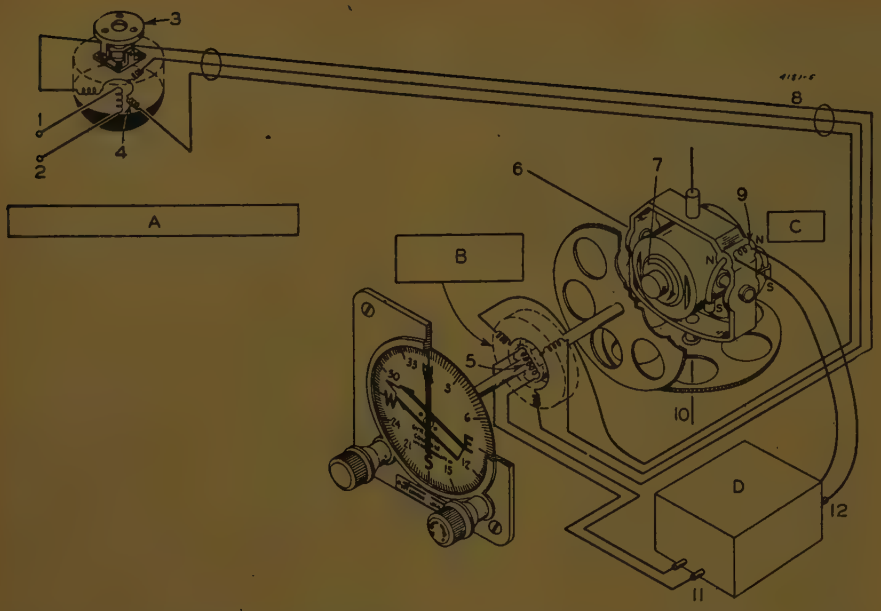


Figure 5. Schematic diagram of flux valve, self-synchronous units, gyroscope, amplifier, and precessor

A—Pendulously mounted flux valve
B—Signal transformer
C—Gyroscope
D—Amplifier
1—400 cycles
2—Excitation
3—Mounted to wing structure
4—Signal pickup coils
5—Rotor coupled to gyroscope
6—Horizontal axis
7—Gyro rotation
8—Connecting cable
9—Precessing coil
10—Vertical axis
11—Input: 800 cycles
12—Output: reversing-polarity direct current

Compensation

Primarily the size of the flux valve lends itself to mounting in very small recesses anywhere in the skin of the airplane. Therefore, in a number of types of airplanes, a location sufficiently remote from magnetic disturbances is available such that compensation is not required. This has been the experience with several standard types of airplanes, in which experimental Gyrosyn compass installations had been made. However, there may be some applications in which local deviations are obtained in every possible location of the flux valve. In these cases compensation must be resorted to. Conventional permanent-magnet compensators are available and add only a fraction of an inch to the over-all dimensions of the flux valve. Because the flux valve naturally will be located in a fairly inaccessible part of the craft, compensation by manual adjustment at that point may be impractical. It is possible to make available means for overcoming this difficulty. This is a method of applying compensation to the magnetic field by remote adjustment. A compensator consists of electromagnets distributed properly around the flux valve. The current in these coils is controlled by rheostats located conveniently in the cockpit or cabin. The compass may be swung either on the ground or in the air and compensated in a straightforward manner by adjustment of the potentiometers. This system is shown schematically in Figure 6. In cases where electric circuits are in close proximity to the valve, these circuits may be linked with the compensation circuit in such a way that full correction is obtained either "on" or "off."

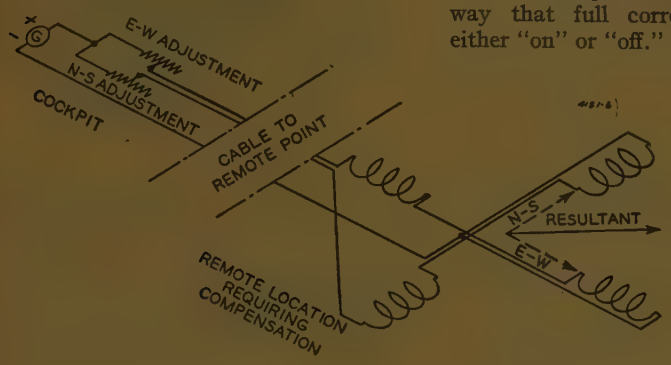


Figure 6. Compensation schematic diagram

Polar Navigation

The Gyrosyn compass obtains reliable operation in horizontal-field strengths greater than 0.04 oersted. However, in regions near magnetic poles, this field strength fades to the vanishing point. In the same region, directional indications obtained from the earth's field are unreliable and difficult to interpret, because of rapid changes in variation. These rapid changes make it necessary to obtain impossibly accurate position data to determine the correct local variations. This loss in magnetic directional information is not particularly serious to the Gyrosyn compass, since the flux-valve control may be turned off, leaving the directional gyroscope as a free gyroscope. Therefore, headings can be substantially maintained for hours or more of flight until reliable magnetic information again can be obtained through the earth's magnetic field. The flux-valve control of the gyroscope will be turned on and navigation resumed in a normal manner. In summing up the characteristics of the Gyrosyn compass in operation over widely varying field strengths, it is indicated that operation without adjustment is obtainable down to the region where magnetic indications become totally unreliable. Then, the precessing control of the gyroscope is suspended and a free navigational directional gyroscope remains.

Conclusions

The directional gyroscope has served many years usefully as an aircraft instrument. It has well-known shortcomings, but it fills a tremendously important place in the field of navigation. Its days of functioning as a free gyroscope subject to random wandering, however, are numbered. From now on, the directional gyroscope is going to be continuously controlled, and the direction-indicating instrument will require no more attention and manipulation from the pilot than the attitude-indication instrument, namely, the artificial horizon, has for a number of years. It is believed that the system described herein contributes a fair share in bringing about this change. No development of this sort can be said to be successful until it is received with approval by the armed services, airplane builders, and commercial operators, and most important, by the pilots themselves. The requirements for remote-indicating compasses that have been stated are an interpretation of the composite demand from all of these groups. It is believed, therefore, that in adhering to this set of requirements a satisfactory directional indicator is produced.

Electric Automatic Pilots for Aircraft

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AUTOMATIC pilots have been in use for many years in both commercial and military aircraft and are well suited to normal airplane control. Their greatest usefulness is in maintaining heading and attitude over long periods of time, with consequent reduction in strain on the human pilot.

Recently, however, with the development of bombardment tactics, the need for precise control has become more important. The functions of an automatic pilot for these applications are such that the maintenance of course, attitude, altitude, and trim are at times of paramount importance. It is necessary that the airplane, through the medium of the automatic pilot, respond quickly to turns as called for by the bombardier. Furthermore, it is desirable that the performance deteriorate as little as possible when the airplane is subjected to the disturbing action of turbulent air. This paper discusses the problems associated with such control and describes some of the features of a recent design.

Attitude Control

The minimum requirement of any automatic pilot is the angular stabilization of the airplane about three mutually perpendicular axes passing through the airplane's center of gravity and angularly fixed with respect to the surface of the earth. Motions about these axes are described as roll, pitch, and yaw. Rolling is rotation about the fore and aft, or longitudinal axis; pitching is rotation about the transverse, or lateral axis; yawing is rotation about the vertical, or directional axis. These motions are illustrated in Figure 1. Under certain conditions, the problem of simultaneous control about an airplane's three rotational axes can be reduced to three independent control systems. Each control system is then similar, and discussion will in general be confined to one axis.

Control of an airplane, manual or automatic, is accomplished through the deflection, or angular rotation of the airplane's control surfaces about the surface hinge line. Rotation of a control surface is, in effect, a change in angle of attack of the surface and gives rise to an aerodynamic force normal to the surface. The control surface forces, in turn, create control moments about axes through the center of gravity of the airplane. The angular acceleration caused by a control moment continues until the resulting damping and stability moments are equal to the control moment. The angular rotation tends to cease when the control moment is equal to the static stability moment. A given control moment therefore is opposed by an inertia moment, a damping moment, and a static stability or spring moment. The magnitude of these moments varies considerably between axes and for different flight conditions. If control moment is maintained until appreciable changes in flight path occur other moments are generated which, however, may be disregarded here.

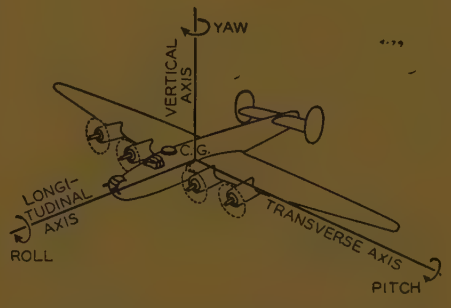


Figure 1. Schematic drawing of airplane showing its three translational axes and the three angular degrees of freedom

For short-period motion, the application of pitching-control moment and the resulting rotation about the transverse axis may be expressed as

$$M_c = I\ddot{\theta} + C\dot{\theta} + K\theta \qquad (1)$$

where

M_c = applied control moment about the transverse axis
I = moment of inertia of the airplane about the transverse axis
C = damping coefficient, or damping moment per unit rate of pitch
K = stability coefficient, or restoring moment per unit angle of pitch
θ = angular displacement in pitch

The foregoing equation is subject to several restrictions. It is approximately correct for small angles of pitch and describes the angular motion only for a short time after control moment is applied. The static longitudinal stability, actually a function of change in angle of attack, is assumed to depend on the angle of pitch. It is further assumed that the coefficients C and K are constant for a particular condition of flight.

For automatic stabilization, the control moment becomes a restoring moment, M_r, and is made a function of the angular error, θ, between the airplane attitude and a gyroscopic reference axis. The restoring moment applied by the automatic pilot then acts to reduce the error to zero in a manner determined by the combined characteristics of airplane and automatic pilot. Assume that the airplane is coerced from its equilibrium position and then released. It is then acted upon by the restoring and the static stability moments such that

$$M_r = f(\theta)$$

and

$$-f(\theta) - K\theta = I\ddot{\theta} + C\dot{\theta} \qquad (2)$$

If the restoring moment is proportional to the error alone, as in many automatic controls,

$$M_r = f(\theta) = K'\theta$$

and

$$-(K + K')\theta = I\ddot{\theta} + C\dot{\theta} \qquad (3)$$

The static restoring moment, $K'\theta$, supplied by the automatic pilot, acts to in-

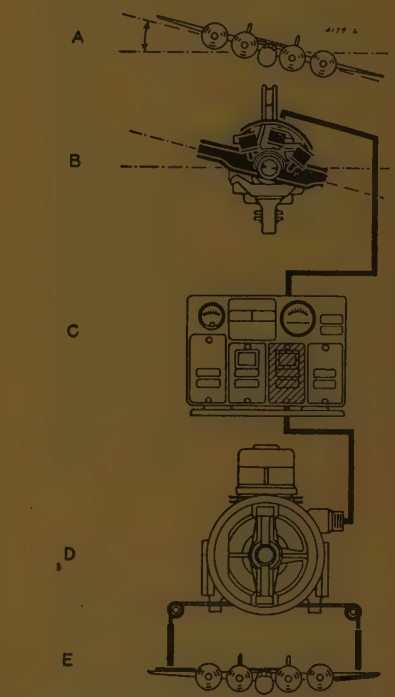


Figure 2. Pictorial representation of the A-5 automatic pilot controlling the airplane in roll

A—Roll angle
Airplane deviates in roll
B—Vertical gyroscope—horizontal gimbal ring and roll pick-off measure roll angle
C—Amplifier rack—roll signal is amplified and derivatives are taken
D—Servo unit deflects ailerons
E—Airplane returns to level flight

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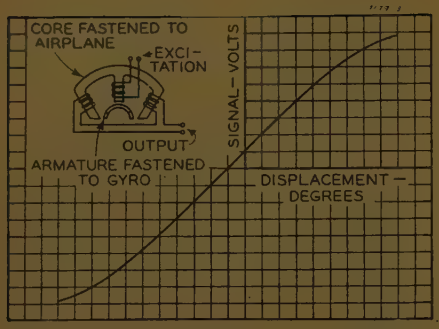


Figure 3. Schematic diagram of signal pick-off and its voltage–angle characteristic

crease the static longitudinal stability, $K\theta$. Where "tight" control is necessary, the stability furnished by the automatic pilot must be several times larger than the inherent stability of the airplane. Control which is proportional to the displacement error alone is known as error control.

When K' is sufficiently large, and

$$C<2\sqrt{(K+K')I}$$

the angular motion is a damped oscillation and undamped natural frequency is

$$f_n=\frac{1}{2\pi}\sqrt{\frac{(K+K')}{I}}$$

Reasonably large values of static restoring moment are desirable for several reasons. The roll and yaw axes of an airplane are mutually dependent; that is, there exists appreciable rolling moment due to yaw and rate of yaw, and yawing moment due to rate of roll and sideslip. The two axes may be considered independent, both from the standpoint of analysis and in the actual case, only if each is under tight static and dynamic control. In other words, if tight rudder control is provided, there can be little rolling moment due to yaw, since there can be very little yaw, and there can be little yawing moment due to sideslip if rolling motion is suppressed. Large values of static restoring moment are necessary too, when any sustained disturbance is encountered, such as an engine failure in multiengine ships; a sudden sustained change in trim, as when a large bomb is released; or a long-period air disturbance.

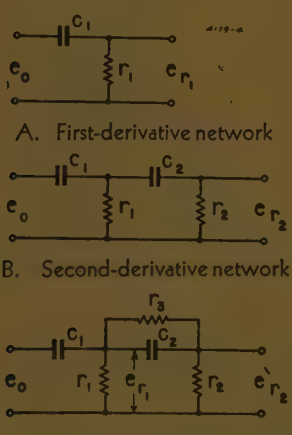


Figure 4. Derivative-taking networks

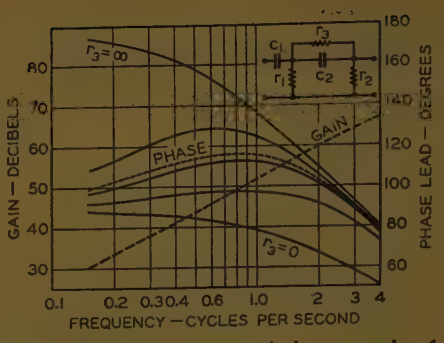


Figure 5. Phase curves of the network of Figure 4C for different values of r_3

The broken curves are measured phase and gain for the rate channel of the amplifier

Additional damping moment is obtained by making the restoring moment proportional to the time derivative of the angular error as well as the error itself, so that

$$M_r=f(\theta,\dot{\theta})=K'\theta+C'\dot{\theta}$$

and, for the condition of an airplane disturbed from its equilibrium position by external forces and then released,

$$-(K+K')\theta=I\ddot{\theta}+(C+C')\dot{\theta}$$

The return of the airplane to its equilibrium position is critically damped if sufficient moment proportional to the first time derivative of the angular error is available, or

$$(C+C')=2\sqrt{(K+K')I}$$

This type of damping is often called error-rate damping.

The return of the airplane to level attitude after it has been subjected to a disturbance must be at least critically damped because of the nature of the disturbances encountered in rough air. Disturbing moments, having frequency components varying over the range from nearly zero to that well beyond the natural frequency of the control, may be encountered. If the system is not critically damped, air gusts having frequency components near the natural frequency of airplane and its automatic control may force abnormal deviations, and what appears to be continuous oscillation results.

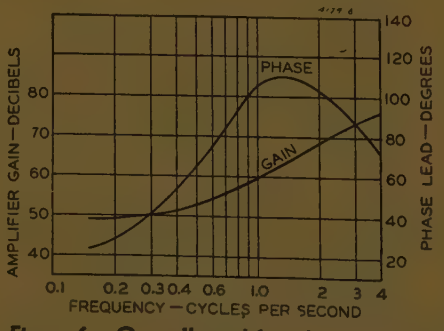


Figure 6. Over-all amplifier phase and gain curves

The requirement of large static restoring moments imposes the necessity for rate control. The damping provided by rate control limits the angular deviations due to air gusts, and makes possible close automatic control in rough air. If the airplane is subjected to a sinusoidal gust or disturbing moment,

$$M_d=M\sin\omega t$$

the resulting maximum deviation is given by

$$\theta_m=\frac{M}{\sqrt{(C+C')^2\omega^2+[I\omega^2-(K+K')]^2}}$$

A properly proportioned static and rate control together with the airplane's inertia will go far to suppress the deviations throughout the entire range of disturbing frequencies.

For very slow disturbances where ω approaches zero,

$$\theta_{m(\omega\to 0)}=\frac{M_m}{K+K'}$$

and, for the condition of critical damping,

$$\frac{\theta_m}{\theta_{m(\omega\to 0)}}=\frac{1}{1+\left(\frac{f}{f_n}\right)^2}$$

where f_n is the undamped natural frequency. Since the ratio can never exceed unity, the deviations are greatest for disturbances that approach zero frequency. The deviations are mainly limited by the static restoring moment at very low frequencies, by the damping at frequencies near the natural frequency, and by inertia at high frequencies.

The foregoing discussion assumes that control moments proportional to the angular deviation and rate of deviation are applied to the airplane with no time lag between their occurrence and the correcting moments. The measurement of error electrically from a free gyroscope is accurate and instantaneous. The derivation of rate of error from the original displacement error can be obtained electronically with any desired response, depending upon the permissible amplifier gain. However, some time delay is usually present in the process of power amplification and movement of the control surface, principally because of power limitations on the servo unit. Time delay in the application of control moment is important only in relation to the time of response required of the airplane. Where tight control is essential, time delay is usually the limiting factor. Error control, if applied through a system involving appreciable time delay, may cause continuous oscillation, depending upon the damping provided, since the

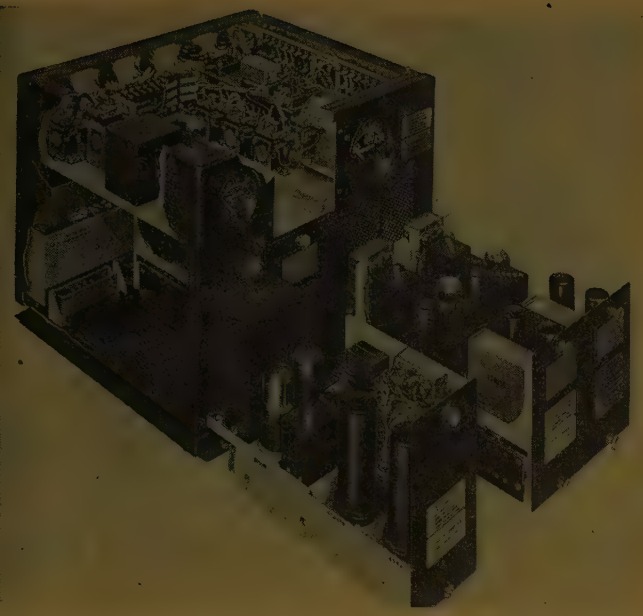


Figure 7. The amplifier rack with removable flight amplifiers

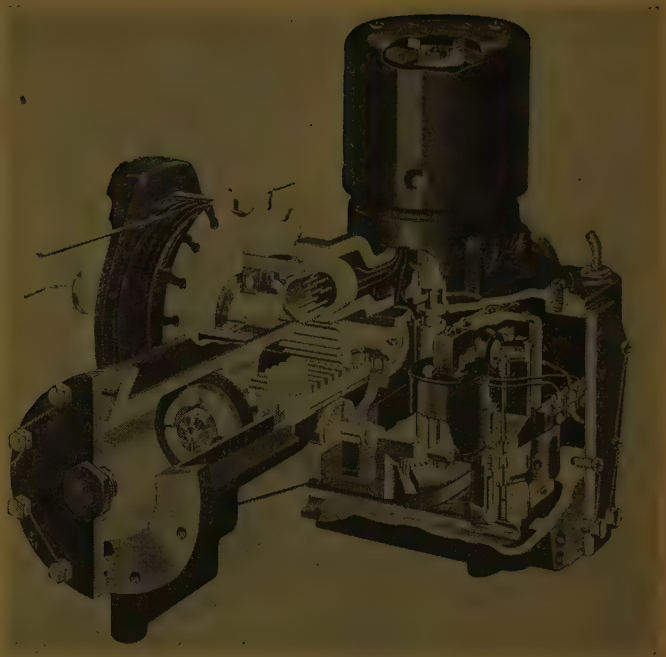


Figure 9. Cutaway view of the servo unit

restoring moment then has a component n quadrature (instead of exactly 180 degrees out of phase) with the airplane's angular position, and there is some net energy per cycle supplied to the system. When rate control is applied through time delay, the control moment actually applied to the airplane has a component out of phase with the airplane's angular position. The rate control degenerates partially to error control and the natural frequency may increase considerably, making the need for rate even greater.

If most of the time lag from gyroscope to control-surface hinge moment is concentrated in a single energy-storing system, represented electrically by a series resistance–capacitance network, or mechanically by a viscous friction-spring system, it can be shown that the effect of such lag can be compensated almost completely by the addition of an acceleration or second-derivative signal. The voltage at the input to the servo unit therefore is made proportional to the sum of displacement error and the first and second derivatives of the error.

Error Measurement

Measurement of pitch and roll error is accomplished through the use of inductive "pick-offs" as shown schematically in Figure 3. The primary winding, or center leg, is excited with a 400-cycle voltage. The secondary windings, or outer legs, are connected in series opposing. Angular motion of the airplane with respect to the gyroscope varies the coupling between primary and opposing secondary windings, so that the voltage induced in one secondary winding increases while the voltage in the other decreases. The output voltage is linear with angular rotation and has the phase of the secondary winding with the larger voltage. A curve of pick-off voltage against angular rotation is shown in Figure 3.

This phase-reversing signal pick-off may be described alternatively as a carrier-suppressed or balanced modulator, where the 400-cycle voltage is the carrier and the airplane angular motion is the signal. The carrier is resupplied at the amplifier in the form of a 400-cycle plate voltage to effect "phase detection" or demodulation.

Derivation of Rate and Acceleration Signals

The input signal to the amplifier is a measurement of airplane angular displacement only. This signal is separated into two channels. One channel provides a displacement component, and the

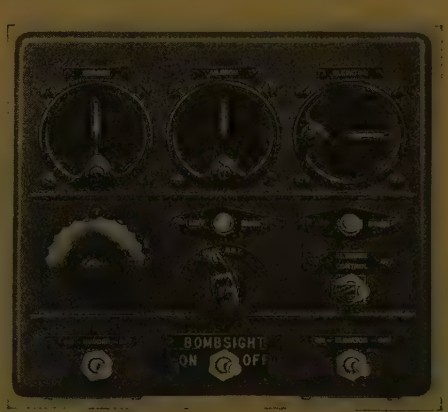


Figure 8. A-5 automatic-pilot control panel

other is phase-detected and applied to the differentiating network of Figure 4C. The voltage e_{r1}, appearing across resistance r_1, is a measure of the time derivative of the demodulated signal voltage e_o. When the single-mesh resistance–capacitance circuit of Figure 4A is considered in more detail,

$$e_o(t) = e_{c1} + e_{r1}$$

If $e_{c1} >> e_{r1}$, then

$$e_o(t) = e_{c1}$$

and

$$i = c_1 \frac{de_o}{dt}$$

therefore

$$e_{r1} = r_1 i = r_1 c_1 \frac{de_o}{dt}$$

If the airplane were hunting at its natural frequency when under automatic control,

$$e_o(t) = E_o \sin \omega_n t$$

and

$$\frac{de_o}{dt} = \omega_n E_o \cos \omega_n t$$

Also,

$$e_{r1} = \frac{r_1 e_o}{\sqrt{r_1^2 + \frac{1}{\omega_n^2 c_1^2}}} \sin(\omega_n t + \phi)$$

where

$$\tan \phi = \frac{1}{\omega_n r_1 c_1}$$

When r_1 is small compared to $1/\omega_n c_1$, ϕ approaches 90 degrees and

$$e_{r1} = r_1 c_1 \omega_n E_o \cos \omega_n t = r_1 c_1 \frac{de_o}{dt}$$

Therefore, when the capacitative reactance, x_{c1}, at the natural frequency of the control system is large compared to r_1, the phase of e_{r1} is advanced nearly 90

Table I. A-5 Automatic-Pilot Sensitivities in the B-24E

Axis	Control Surface	Pounds Cable Force Per Degree Of Error
Roll	Aileron	40
Pitch	Elevator	48
Yaw	Rudder	63

degrees ahead of e_o. A similar phase lead is possible with a series resistance and shunt inductance, but at these frequencies the large reactance required for effective vacuum-tube operation makes the physical size of the inductance prohibitive.

If we refer to Figure 4B, by the same reasoning e_{r_3}, the voltage across r_2 is a measure of the second derivative of e_o, provided the impedance $\sqrt{r_2^2 + x_2^2}$ at the natural frequency is large compared to r_1. Any desired combination of de_o/dt and de_o^2/dt^2 is obtained by shunting C_2 with the resistance r_3 as in Figure 4C. Phase curves for the network of Figure 4C and measured phase and gain curves for the rate channel of the amplifier are shown as a function of frequency in Figure 5. With r_3 short-circuited, the phase lead approaches 90 degrees at zero frequency; and with r_3 open-circuited the phase lead approached 180 degrees at zero frequency. Phase and gain curves for the entire amplifier are shown in Figure 6.

Servo Unit

The amplified displacement and first- and second-derivative voltages are impressed across the input terminals of the servo unit, or power unit. The servo unit is a self-contained electrohydraulic system which applies cable force proportional to amplifier-output voltage. A hydraulic-pressure repeat-back is incorporated in the servo unit whereby the hydraulic pressure developed in the cylinders, acting across a small valve-stem area, is matched against the force developed by an electromagnetic torque motor. For a given voltage applied to the torque motor, the servo unit tends to maintain constant pressure in the cylinders, regardless of drum position. The servo drum operates at maximum speed until the force balance is attained. Since for a given amplifier output the servo unit tends to maintain a proportional force output, when no signal voltage is applied, it tends to maintain zero force output, and the servo drum moves freely when subjected to external forces. While the servo unit is capable of providing the large forces and speeds required, the average power drawn from the airplane's power supply is kept at a minimum, because pressure is developed only when called for by the signal.

Force Repeat-Back

The force repeat-back principle of aircraft control, as provided by the servo unit, merits some discussion as to the reasons for its use. The aerodynamic force acting on a control surface depends upon the angular deflection of the surface from its neutral or weather-vane position. Neglecting the small positive and negative damping forces acting on the surface because of its rotation and rotation of the airplane, this force is proportional to the surface deflection for small angles. If the airplane's cable-system friction is neglected, therefore, the surface force, hinge moment, and cable force are proportional to their respective deflections.

The control-system frictional forces normally are kept small in order that the human pilot may retain the "feel" of the airplane. Satisfactory manual control is obtained when the ratio of aerodynamic force to friction force is high. The principal load on the control cable therefore may be considered that of a spring. The zero point of the spring, however, shifts over a wide range, depending upon power, loading, and any unsymmetrical conditions which may arise. The spring stiffness is not constant but increases as the square of the air speed. A given force applied to the control cable will result in different deflections of the surface depending upon the air speed, but will result in a fixed control moment independent of air speed. When the servo member of the automatic pilot is made to exert a force on the control cable proportional to error signals, tight control may be retained throughout a large speed range.

Automatic Trim

When the center of gravity of an airplane shifts longitudinally, or when the propeller thrust or the air speed is changed, moments are created about the transverse axis which require a steady elevator-control-cable force for their compensation. Constant biasing moments such as these are normally furnished by the elevator "trim tab." By means of the tab the elevator is shifted to a new angular position, reducing the elevator-cable force to zero. Since the control-surface position at which zero resultant control moment occurs is variable, it is convenient that the servo force be independent of servo position.

Automatic operation of the trim tab in a manner to maintain the average servo-force zero is provided only in the elevator system, where frequent changes in trim occur. The trim-tab control responds to the average elevator servo force, and hence to the average pitch error, θ. The restoring moment provided by the trim-tab control is approximately proportional to the time integral of the pitch error. The total restoring moment about the transverse axis is then

$$M_T = \int \theta dt + K'\theta + C'\dot{\theta}$$

Control of Altitude and Course

When constant altitude is required, the pitch attitude, and hence approximately the rate of climb or descent of the airplane, is varied according to the alti-

Figure 10. The constant-altitude control

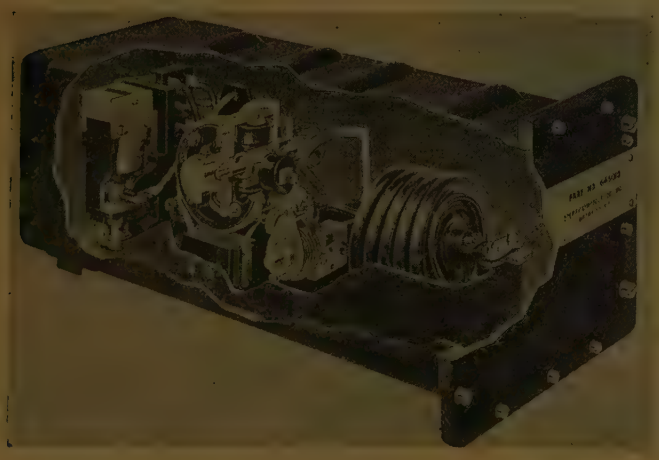
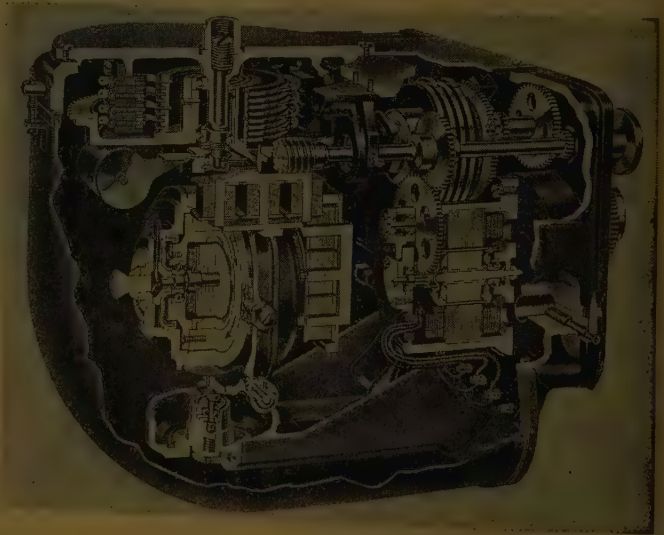


Figure 11. Cutaway view of the azimuth gyroscope



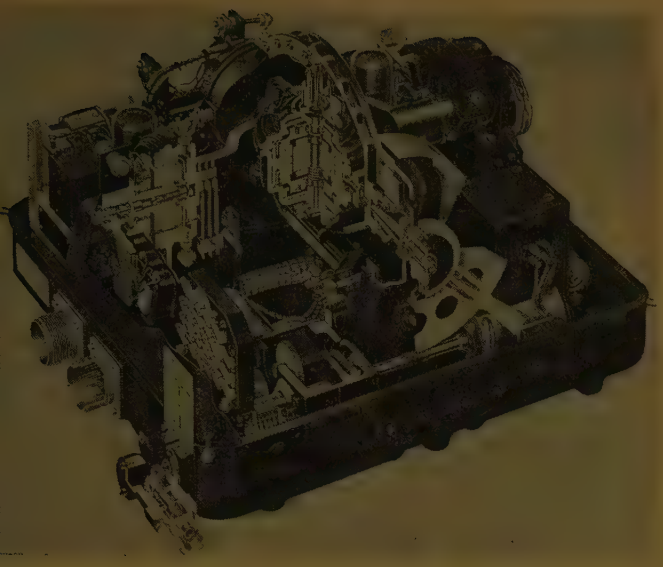


Figure 12. Cutaway view of the vertical gyroscope



Figure 13 (right). The turn control

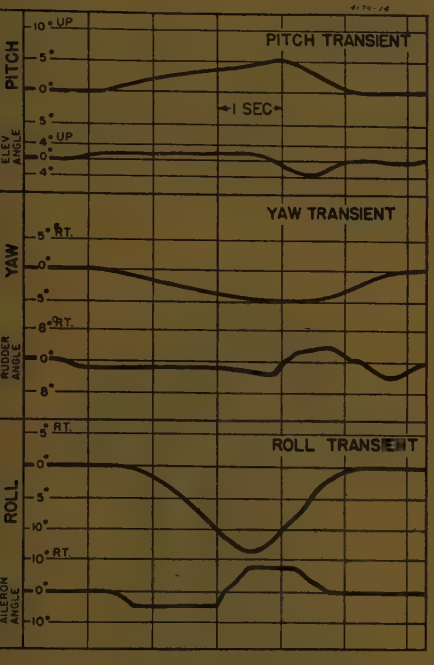


Figure 14. Recordings of transient flight conditions in the *B-24E*

Altitude 6,500 feet; indicated air speed 175 miles per hour

ude error. Since the natural frequency f the airplane position about an altitude eference is low compared to the natural requency of the pitch control, very close ontrol of altitude is possible by using nly altitude error signals. Normal sen-itivity of the altitude control is one de-ree of pitch change per 20 feet of alti-ude error. The altitude reference is a arometric element, and changes in static ressure are caused to modulate an al-ernating voltage in a way similar to that lready described. This altitude-error ignal voltage biases the pitch signal to provide the required changes in pitch attitude. Figure 10 shows a cutaway drawing of the altitude control.

The airplane is maintained on course by setting in heading changes according to departures from some reference on the ground. Heading changes may be very small or may consist of turns of more than 360 degrees. For this reason, heading errors are measured by a synchronous signal transformer, the stator of which is driven from the vertical ring of the azimuth gyroscope. The stator windings of this signal transformer are connected to the stator of an identical unit in the turn control, the rotor winding of which provides the heading-error signal. Either rotor may be rotated to shift the azimuth index, thereby effecting a change in heading. The airplane is automatically banked by shifting the roll index an amount corresponding to the rate of change of heading or rate of turn. The airplane responds quickly to a change in azimuth index, for the control moment provided by the rudder is proportional both to the amount of index change and to the rate of index change.

Performance

The performance of the automatic pilot is best indicated by giving static sensitivities in cable pull per degree of airplane deviation, and reproducing records of transient flight conditions. A table of sensitivities for a typical installation in a *B-24E* "Liberator" is given in Table I.

The flight records are measurements of attitude error and control-surface position in their correct time relationship. The "flight recorder," in measuring attitude, has a dynamic error of less than one tenth of one degree under these conditions of operation. Surface position is recorded by direct mechanical connection to the cable system.

A transient is put into the system by manually overpowering the automatic pilot, holding this condition until considerable attitude error is established, and suddenly removing the manual effort. Typical transients are shown in Figure 14. In each case there is a relatively long period of approximately constant control-surface deflection while the error is increasing. At the end of this period, the system is freed and the error becomes zero in a relatively short time. In each case the airplane returns to its original attitude in an approximately critically damped fashion. The records show that the damping supplied by the automatic pilot is greatest in yaw, a fact which should be expected since the airplane has its greatest inertia and least damping around its vertical axis.

Conclusions

An airplane may be stabilized accurately about its three rotational axes, provided proper error and error-derivative measurements are utilized. High static restoring moments are applied by the automatic pilot with sufficient derivative control to provide approximately critical damping. The static resorting and damping moments supplied by the automatic pilot, together with the airplane's inertia, effectively suppress the deviations over the full range of disturbing frequencies due to rough air. The force repeat-back system permits tight control over a wide range of air speeds and provides a convenient method of automatically maintaining elevator trim.

Solenoid-Operated Hydraulic Valves for Aircraft Applications

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NONMEMBER AIEE

DURING the past several years there has been a definite trend by some aircraft manufacturers to eliminate hydraulic controls from combat airplanes. The vulnerability of hydraulic systems used on airplanes manufactured during this period and to a certain extent today are big factors in promoting this trend. Space requirements in the cockpit of an airplane to mount control valves and accommodate necessary piping to these valves, difficulty with packings at extremely cold temperatures, and the lack of flexibility of the control system are other factors that contributed to the aforementioned trend.

The changeover, however, has been slow because hydraulically operated controls have a number of advantages over other types in applications where considerable work must be done during a short period of time.

As soon as the shortcomings of the standard hydraulic systems relative to combat airplanes became apparent, considerable effort was applied to overcome them. Improved packings for low-temperature operation, hydraulic fuses that isolate any portion of a hydraulic system that has a ruptured hydraulic line, and lightweight solenoid-operated control valves, the advantages and description of operation of which will be given in detail later, are all recent developments that should bring about further consideration of hydraulic controls in applications in which considerable power is required.

Some of the major advantages of solenoid-operated valves are as follows:

1. Manually operated valves require either a rotary or reciprocating shaft for plunger or poppet actuation; therefore, seals are required at the point at which the shaft protrudes from the valve proper. Forces to move poppets or plungers in most solenoid-operated valve designs are transmitted into the valve without any mechanical linkage from outside the valve proper; therefore, all except static seals are eliminated. Although considerable work has been done on hydraulic seals in recent years they still present a troublesome service problem because of deterioration and wear.

2. Manually controlled valves must be accessible; therefore, they are mounted at the control point, such as the cockpit of an airplane. A visual inspection of the control panel of an airplane will show a maze of dials, buttons, and levers on the front side of the panel, and an even greater maze of wires and hydraulic piping at the rear. A solenoid-operated valve may be actuated by a simple switch on the control panel and may be placed in a position that permits short hydraulic line lengths. The total weight and vulnerability of the system consequently are greatly reduced.

3. Since solenoid-operated valves are electrically controlled, they lend themselves to semiautomatic and automatic operation. This feature is of particular importance in combat airplanes where automatic operations will relieve the pilot of at least some mental and physical effort. Automatic operations in which solenoid-operated valves are used are now under test by the Army Air Forces.

Although solenoid-operated valves have been used rather extensively in manufacturing processes and machinery, they are comparatively new in aircraft applications. The application of solenoid-operated valves to aircraft presented many new problems that were not present in previous applications. Some of these problems and means used to overcome them are as follows:

1. Plungers on plunger-type valves may be pressure balanced, so that they can be moved relatively easy, but these valves are difficult to make with leakage rates low enough to be acceptable to the aircraft industry. The leakage rate of a plunger-type valve also increases with usage as the plunger and valve body wear, even if it is hand fitted or lapped to give a low rate of leakage initially. Poppet valves eliminate the leakage objection; however, if the poppets are large enough to permit satisfactory flow at permitted pressure drops, the forces required to unseat the poppets at pressures now used in aircraft are so high that a solenoid designed for this purpose would be too large to be practical for aircraft usage.

2. To bring the forces required to operate poppet valves within reasonable limits, pilot valves are used. Pilot valves are also poppet valves, but port areas of these valves are comparatively small, so that hydraulic forces tending to hold them closed can be overcome readily by a solenoid of reasonable size. Opening of the pilot valve initiates movement of the main poppet by causing a pressure differential on its opposite ends. A detailed description of this action will be given later.

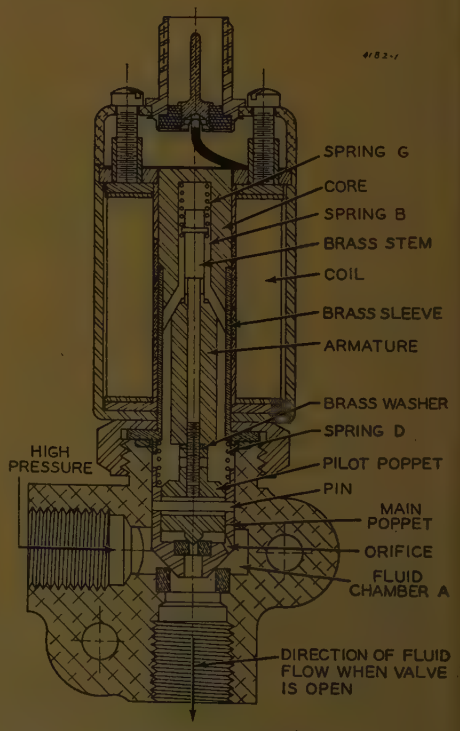


Figure 1. Solenoid-operated normally closed two-way hydraulic valve

A valve of this type weighing approximately one pound will function continuously at pressures as high as 2,000 pounds per square inch. The solenoid will draw one ampere at 24 volts. At a flow of five gallons of AN-VV-O-366b fluid per minute the pressure drop will be 20 pounds per square inch

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When a satisfactory method of angular control is established, the control of altitude and course according to any desired reference is readily attained.

References

1. DIRECTIONAL STABILITY OF AUTOMATICALLY STEERED BODIES, N. Minorsky. *Journal*, American Society of Naval Engineers, volume 32, number 2, May 1922.

2. THEORY OF SERVO-MECHANISM, H. L. Hazen. *Journal*, Franklin Institute, September 1934.

3. DESIGN FACTORS CONTROLLING THE DYNAMIC PERFORMANCE OF INSTRUMENTS, C. S. Draper, G. P. Bentley. *Transactions*, American Society of Mechanical Engineers, volume 62, number 5, July 1940.

4. AEROPLANE STABILITY AND THE AUTOMATIC PILOT, F. W. Meredith, P. A. Cooke. *Journal*, Royal Aeronautical Society (London, England), volume 41, 1937.

5. DESCRIPTION OF THE SPERRY AUTOMATIC PILOT, E. A. Sperry, Jr. *Aviation Engineering*, volume 6, January 1932.

6. STABILITY OF AIRPLANES AND AUTOMATIC FLIGHT, Stephen J. Zand. *Aviation*, June 1944.

7. THE GYROSCOPE; ITS PRACTICAL CONSTRUCTION AND APPLICATION (book), P. P. Schilovsky. Chemical Publishing Company, Brooklyn, N. Y., 1938.

8. AERODYNAMICS OF THE AIRPLANE (book), Clark B. Millikan. John Wiley and Sons, Inc., New York, N. Y., 1941.

3. The development of hydraulic oils with comparatively flat viscosity curves also has promoted the use of pilot-operated valves. Pilot operation of valves depends on pressure differentials brought about by opening at least one orifice to permit escape of fluid in sufficient quantities to cause a pressure drop. The chamber from which escape of fluid is permitted is also supplied fluid through a small fixed orifice and clearance around the poppet. As temperatures decrease the oils become more viscous, thereby affecting the flow of fluids through the orifices and clearances, which in turn affects the differential in pressure used to move the main poppet. However, if the viscosity can be kept within reasonable limits the temperature range in which the valve will function satisfactorily is adequate.

A solenoid-operated hydraulic valve design that has proved quite satisfactory is shown in Figure 1. This design is a pilot-operated poppet type. With reference to Figure 1, this valve functions as follows:

We will assume that the valve is closed with fluid under pressure in fluid chamber *A*. Fluid in the space above the main poppet, pilot poppet, and armature also will be under pressure, since this space is open to chamber *A* through the orifice in the main poppet and clearance around the pilot poppet and armature. In the valve shown, the main poppet requires a tractive effort of approximately 120 pounds to unseat it at 1,500 pounds per square inch pressure, whereas, the pilot poppet requires a tractive effort of only three pounds to unseat it at this same pressure.

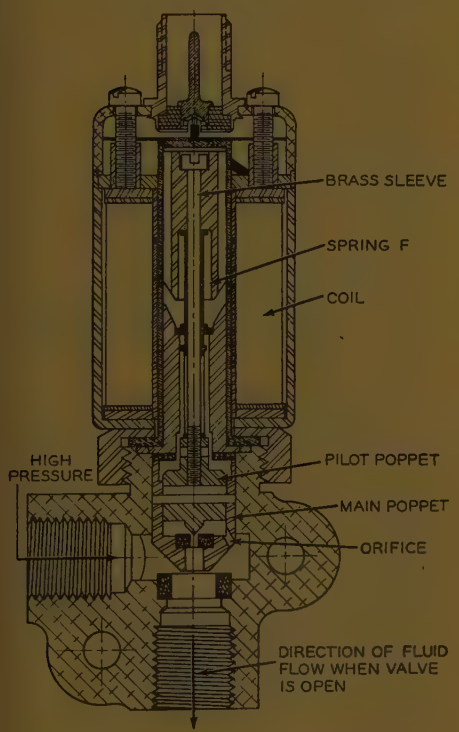


Figure 2. Solenoid-operated normally open two-way hydraulic valve

The size, weight, and functional characteristics of this valve are the same as those described for the valve shown in Figure 1, except that the maximum opening pressure is 1,700 pounds per square inch

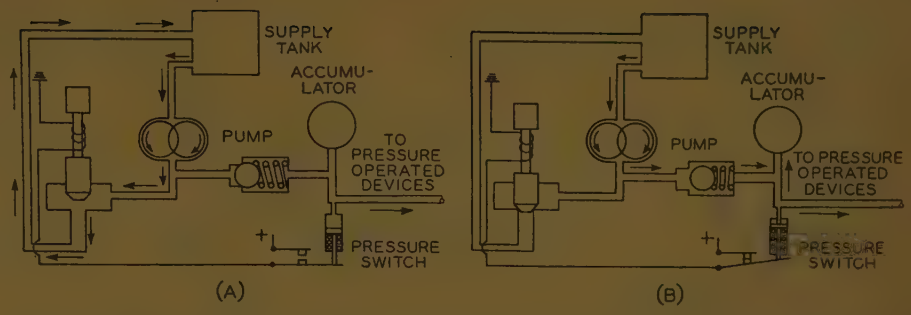


Figure 3. Control circuit for a normally open solenoid-operated valve used as a pressure regulator

A. Solenoid-operated valve in normal position. Pump by-passing fluid at no pressure

B. Solenoid-operated valve in energized position. Pump-filling accumulator

When the coil is energized the armature will move along the brass stem away from the brass washer at the bottom toward the core. During movement of the armature, spring *B* is compressed. At a point where the air gap between the armature and the core is slight, the armature strikes an annular shoulder on the brass stem attached to the pilot poppet and unseats it. Unseating of the pilot poppet permits escape of fluid from the space above the main poppet. Although a small amount of fluid flows into the space above the main poppet through the orifice shown, the pressure drop in this space will be sufficient, so that the high pressure on the area below the main poppet will move it upward. The armature then will be in direct contact with the solenoid core. Spring *B* which was compressed during initial movement of the armature, then will move the brass stem attached to the pilot poppet upward. The pilot poppet which is attached to the main poppet by means of a pin that permits approximately 0.040 movement between them picks up the main poppet and opens it wide. Spring *C* is comparatively light, and spring *B* must be stronger than spring *D* so as to permit full opening of the main poppet. The total time required for opening this valve is less than one-tenth second at normal temperatures.

When the coil is de-energized spring *D* will seat the large poppet and spring *C* will seat the pilot poppet.

Figure 2 shows a valve of similar design except that it is open in the unenergized position. When the coil is energized the armature moves downward, compressing spring *F*. As the armature moves downward it seats both the pilot poppet and main poppet. When both poppets are seated there is a gap between the armature and the head of the brass stem on which the armature slides. When the coil circuit is opened spring *F* moves the armature upward. The force of spring *F* and the impact force of the armature then will open the pilot valve. The maximum opening pressure of this valve is dependent on spring *F*. Sequence of operations to open the valve fully from this point are the same as those described for the normally closed unit.

The impact feature built into these valves adds approximately 30 per cent to the maximum pressures at which these valves open.

Various combinations of two-, three- and four-way valves with ports normally open, normally closed, or a combination of normally open and normally closed can be manufactured to fit any particular application.

Some typical circuits that may be used to control the operation of these valves are shown in Figures 3 and 4.

Figure 3 shows a normally open solenoid-operated valve, same as that shown in Figure 2, used in conjunction with a pressure switch to regulate the flow of fluid to a hydraulic accumulator. A snap-action switch with a slight range of travel between the "off" and "on" position, for instance, might keep the accumulator charged to a pressure of from 800 to 1,000 pounds per square inch. Pressure switches of this type are now available.

Figure 4 is a schematic drawing showing a three-way valve with a normally closed pressure poppet and a normally open exhaust poppet used to charge automatically a gun of the 0.50-caliber *M*-2 Browning type. This system will automatically charge a gun, when it fails to fire, if the trigger switch is depressed. It also provides means to "safety" the gun, that is, hold the bolt in the retracted position so that it cannot be fired.

The hydraulic system, with the exception of the three-way valve, is common to all airplanes that have hydraulic systems. The trigger switch, firing solenoid, and firing relay are common to all airplanes having an electrical firing control.

Three relays and an electrolytic condenser are required in the control box. One control box is required per gun. A normally open switch, that is, actuated by the gun bolt, is mounted on a hydraulic actuating cylinder mounted on the gun.

Any number of guns may be controlled from one common switch in the cockpit; however, a control box, hydraulic actuating cylinder, gun switch.

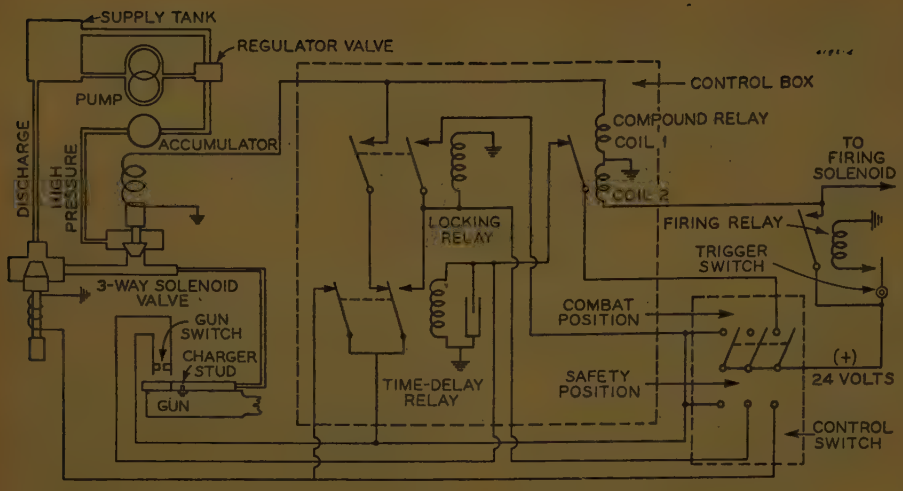


Figure 4. Control circuit for a fully automatic gun-charging system plus means to "safety" guns

and three-way valve are required for each gun.

When the control switch is placed in the "combat" position the time-delay relay is energized by a closed circuit through the control switch and the normally closed contacts of the compound relay. The normally open set of contacts that controls the locking relay coil therefore is closed, the locking relay coil energized, and its normally open sets of contacts closed. The only purpose of the locking relay is to prevent a partial charging cycle when the combat switch is closed initially. From this point on the locking relay will be energized until the setting of the control switch is changed. The two sets of normally closed contacts of the time-delay relay that control the coil circuits of the valve are then open. The system will stay in this condition until the firing switch is closed.

When the trigger switch is closed, coil number 2 of the compound relay is energized, thereby opening its contacts and breaking the circuit to the time-delay relay coil. The normally closed contacts of the time-delay relay will not close immediately because of the condenser across its coil. If the gun then fires, the charger stud will actuate the gun switch once per round fired, thereby maintaining the time-delay relay in the energized state with current impulses. If the gun fails to fire after a predetermined time interval, the two pairs of normally closed contacts of the time-delay relay will be closed and both coils of the valve energized. The exhaust port of the valve then will be closed and the pressure port open. Fluid under pressure is then free to flow into the actuating cylinder which retracts the bolt to its fully recoiled position. In the latter position the gun switch will be made, thereby closing the circuit to the time-delay relay which opens the circuits to the solenoid coils of the valve, which again

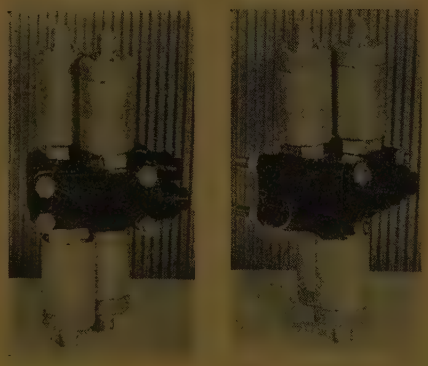


Figure 5. Solenoid-operated four-way hydraulic valve commonly used to control a double-acting cylinder

Weight—4¼ pounds. Will handle flows up to six gallons per minute at a maximum pressure drop of 35 pounds per square inch

assumes its normal position and permits the fluid in the actuating cylinder to return to the supply tank. A quick release mechanism on the charging cylinder holds the gun bolt in the recoiled position and keeps the gun-switch circuit closed until fluid in the actuating cylinder is very nearly exhausted. If the gun does not fire again in the predetermined time after completion of a charging cycle, the operation will be repeated if the firing circuit is still closed.

Relay-coil number 1 of the compound relay assures completion of any charging cycle that is initiated, irrespective of the condition of the firing trigger. This coil is hooked into the coil circuit that controls the high-pressure poppet and can be opened only when the gun switch is made at the end of the actuating-cylinder travel.

When the control switch is placed in the "safe" position, the locking relay is immediately energized, and, since the contacts at the time-delay relay are closed, the coil that controls the high-pressure poppet will be energized to unseat it and permit fluid under pressure to flow into the actuating cylinder. The circuit to the coil that controls the exhaust poppet is closed directly as the control switch is put into the "safe" position and, therefore, will remain seated as long as the switch is in this condition. As the actuating cylinder retracts the gun bolt, it will make the gun switch at very near the end of its travel. Closing of the gun switch will energize the time-delay relay coil, the contacts of which will open and break the circuit to the coil that controls the high-pressure poppet which will then seat.

Since the coil that controls the exhaust poppet remains closed as long as the control switch is in the safe position, the gun bolt will be held retracted during this same period. When the switch is put into the neutral or "off" position, the exhaust poppet will be unseated and permit fluid trapped in the cylinder to return to the supply tank.

The aforementioned control circuits have been cited to show the flexibility of controls in which hydraulic power is electrically controlled by means of solenoid valves. Other types of control circuits that permit positioning of an actuating cylinder in an infinite number of positions without hydraulic lines leading to the control point also have been developed.

In conclusion, a combination hydraulic and electrical control may offer better possibilities than either one used alone. For instance, the automatic gun charging unit shown in Figure 4 would be a practical impossibility with a wholly hydraulic control, and a wholly electrical control would probably be heavier and more complex to attain the same functional characteristics obtained by the combination.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

The Scientific Basis for the New British System of Cockpit Lighting

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NONMEMBER AIEE

Synopsis: The new British system uses ultraviolet for the illumination of the instrument markings and red floodlighting for the general illumination of the cockpit. The scientific basis for this system, which is known as the dual system of cockpit lighting, is discussed, and some details of the fittings are given. Particular emphasis is laid on the layout of the cockpit, correct layout being regarded as the most important single factor for the success of the whole system.

In order to operate an aircraft at night, and particularly a military aircraft, with the maximum comfort, safety, and efficiency, the pilot must be able to read his instruments at a glance, but the method used to render the instrument markings legible must not be such as to reduce appreciably his ability to see objects outside the aircraft. Anyone who has tried to look out of a lighted room at night knows that the brightness level inside the room must be low if anything much is to be seen outside. This was realized from the beginning of cockpit lighting, and the markings on the instruments until recently have been self-luminous and could be read, although with some difficulty, without any lighting at all. However, as controls, switches, radio equipment, messages, maps, and so forth also must be observed or read at times, it has in the past been the custom to install a system of general lighting, usually white, for use when required, these lights being controlled by means of dimmers.

Had the right color been chosen for the self-luminous paint and for the lighting, and had cockpits been laid out so as to make it possible to put the lights in reasonable positions for good illumination, then, operationally this system would have been satisfactory. It is, however, the fact that the wrong color, that is, green, was chosen for the self-luminous paint, and that the right color, that is, red, was not always chosen for the lighting. Also, until night fighting forced attention to the problem the lighting was not considered to be important enough to be taken into account in the initial layout of the cockpit, with the result that some types of aircraft were built with cockpits which, without extensive structural alterations, could not be illuminated adequately by any method whatsoever.

It is the object of this paper to show from the properties of the dark-adapted eye what the colors of the markings and of the cockpit illuminant should be, and also to indicate the few simple precautions which have to be taken in the layout of the cockpit and the positioning of the lamps to insure good results. The layout of the cockpit is considered to be by far the most important single factor in cockpit lighting, but for convenience the color of the markings will be considered first.

Color of Markings

It is common knowledge that the ordinary green self-luminous markings cannot be focussed sharply by the normal dark-adapted eye at the distances at which these markings are observed, that is, about 18 inches or more. If the color is changed to orange without altering the brightness of either the markings or their background, it will be found that the markings stand out with a distinctness most surprising to those who for years have put up with the ordinary "fuzzy" green markings. Unfortunately orange self-luminous compounds of sufficient brightness have not been available until quite recently, and little is known as yet of their durability in service. Red self-luminous compounds of sufficient brightness are not available at the present time, and there are reasons for believing that they never will be.

Important as distinctness of markings undoubtedly is, there is still a stronger reason for changing from green to red or orange. In the dark-adapted eye the ratio of parafoveal to foveal sensitivity decreases from about 500 in the blue and 200 in the green, to about 10 in the yellow–orange and 0.8 in the red. When the pilot is looking out of the cockpit, the images of the markings are thrown on the parafovea, that is, are seen in the tail of the eye, and it is this parafoveal sensitivity ratio which determines the distracting effect of the cockpit lighting. In other words, what is wanted are markings of such a color that they appear to come up bright when the observer looks directly at them, but fade out as soon as the observer looks away from them. Deep red meets this requirement very closely, but even orange–red is a very great improvement on blue–green.

The reduction in the distracting effect of the markings is the fundamental reason for changing the color from green to orange, and for selecting red as the color of the general lighting, but there are other important reasons:

(*a*). As the specular reflectivity of the sidescreens is of the order of ten per cent, it follows that reflections of objects inside the cockpit cannot be seen in the sidescreens either foveally or parafoveally if the general illumination of the cockpit is such that the brightness of these objects is less than about ten times threshold. If white were used, these reflections seen parafoveally would appear to have a brightness about the same as the actual objects seen foveally.

(*b*). Because of the relative insensitivity of the parafovea to red, there is little or no danger of scattered light from the cockpit giving away the position of the aircraft to an enemy.

(*c*). The time taken for the eye to pass from the light to the dark-adapted state is very considerably shortened if adaptation takes place in the presence of weak red light instead of in complete darkness. If dark adaptation is lost due to the aircraft being caught in searchlights or through the use of a hand torch in the cockpit, then recovery of dark adaptation is hastened.

(*d*). Recent research indicates that in the presence of weak red light the final state of dark adaptation is not impaired to an appreciable degree, and, indeed, may be slightly better than in complete darkness.

As the last of the afore-mentioned reasons may be questioned, it is worthy of note that a similar result was obtained quite independently in tests using a typical single-seat cockpit fitted with instruments having yellow painted markings and illuminated with weak red light. It was found that after observing these markings, representative targets outside the cockpit could be picked up in times which were about ten per cent shorter

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than were obtained in complete darkness. This result seems to be explainable only on the assumption that the eyes are slightly more sensitive in weak red light.

Layout of Cockpit

All modern operational aircraft have totally enclosed cockpits. The enclosing surfaces must be polished in order to be transparent, and there is no practicable method of preventing these polished surfaces from reflecting whatever objects happen to bear the correct geometrical relationship with the observer's eye. It follows that reflections can be eliminated only by adopting the principle of the reflectionless shop window, that is, by arranging that the important surfaces, namely, the windscreen and those parts of the sidescreens immediately adjacent to it, shall reflect some surface of low brightness, that is, which is painted mat black. This black surface obviously must take the form of a coaming underneath the windscreen, as shown in Figure 1. In new designs such a coaming can be incorporated without detriment to the general layout, and even in existing types of British aircraft no case yet has been found in which a substantial coaming could not be fitted.

As regards the positioning of the lamps, the chief trouble is to avoid reflections of the light source itself in the cover glasses of the instruments on the panel. It follows from the geometry of the layout shown in Figure 1 that the pilot will not see images of the light source if the lamps are mounted as high or higher than the top of the upper row of instruments, and that the correct place to mount the lamps is underneath the coaming. The coaming therefore serves to prevent reflections both in the windscreen and in the cover glasses of the instruments. Where ultraviolet radiation is used the coaming has a further purpose.

It is obvious that the whole inside of the cockpit should be mat black and that the pilot's clothing, and particularly his gloves, also should be black. Instruments should not have protruding bezels or knobs which would cut off the light from other instruments. Markings should be bold, all of one standard size, and should be shaped and proportioned to give maximum legibility. Instruments with markings smaller than standard, if possible, should be grouped nearer the lamps than those with the larger markings. The instruments at the sides and bottom of the panel should be tilted so that neither the pilot's view of them nor the illumination incident on them is unduly oblique.

Methods of Cockpit Lighting

If it is assumed that the cockpit will be laid out and the lamps mounted as described previously, then the simplest solution would be to have the markings in orange self-luminous paint and to use red general cockpit lighting capable of being dimmed to extinction. It easily can be shown that markings which are just bright enough to be seen in complete darkness are bright enough to be seen on moonlit nights and in twilight. In any case, the general lighting system, if properly installed, can be used to boost the brightness of the markings if abnormal conditions arise, as for instance, when the aircraft is caught in searchlights. This paint is already used in a few British instruments, notably the lubber lines on the magnetic compass.

However, even if a satisfactory orange self-luminous paint should be forthcoming, there are objections to it on production and maintenance grounds, and therefore it was decided not to use it in the new British system of cockpit lighting. Red light on plain white painted markings was tried, but it was found that the markings appeared flat, owing partly to the deep-red color and partly to the fact that the contrast between the markings and their background in practice seldom

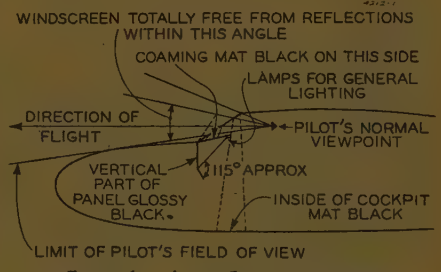


Figure 1. Antireflection coaming

Reflected rays and image of coaming in windscreen shown dotted

exceeds 20 to 1. Also, most existing cockpits contain small miscellaneous objects which are light colored or give small specular reflections. For these reasons it was decided to use orange fluorescent markings and to supplement the red lighting with ultraviolet radiation, thereby obtaining the desired distinctness. This combination also permits both red and ultraviolet to be used at low intensities, thereby reducing troubles from specular reflections due to the red, or from eyeball fluorescence due to the ultraviolet. Also, either system acts as a standby for the other in case of lamp failures. This whole system is known as the dual system and has been adopted as the standard method of cockpit lighting for all operational types of British aircraft.

Another method of rendering the instrument markings legible is to build lamps into each instrument, or light them from behind the panel as in automobiles. Unfortunately aircraft instruments, being of all shapes, sizes, and types, do not lend themselves well to being lighted in this way, and great difficulty is experienced in securing uniform dimming and uniform dial brightness, and in preventing light from leaking out round the edges of the bezels. These leakages can be overcome by the use of ultraviolet in some form, but the great disadvantages of the method as regards production and maintenance still remain, these disadvantages being even more marked in the case of British aircraft which have not hitherto had earth return. In any case, this method has to be used in conjunction with a floodlighting system in order to illuminate the large amount of gear which, in modern aircraft, lines the sides of the cockpit.

Fittings

The chief requirement for the fittings is to keep the size down. The red lamps used in the dual system are 2.4 inches long by 0.75 inch in diameter and accommodate a lamp of three watts. Two of these under the coaming usually can be arranged to flood the whole cockpit, that is, the instrument panel and the two sides. A local light is always required over the magnetic compass, but, since the needle, grid wires, and lubber line are orange self-luminous, this light is only used when setting a new course.

Ultraviolet radiation, if allowed to enter the eye, produces the effect of a blue mist, but in the dual system this is prevented partly by the coaming and partly by screening the lamps. As only a small amount of ultraviolet can be used without detriment to vision, the radiation should be obtained from tungsten filament lamps so as to obviate the difficulties associated with gaseous-discharge tubes, that is, dimming and radio interference. In the dual system two seven-watt fittings have been found sufficient for single-seat cockpits and four for two-seat cockpits.

Since, in the dual system all lights would go out if the aircraft electric supply failed, one additional orange lamp is fitted over the flying instrument panel, and is supplied from a separate alkaline accumulator. The members of the air crew also are issued with hand torches which may be fitted with red bulbs.

Concluding Remarks

The dual system now has been fitted to many types of aircraft and tested by many pilots, and in no case has any report adverse in principle been made.

So many conflicting requirements have to be met in the layout of a cockpit that an entirely happy solution to the lighting problem is not always possible, but there is no doubt that the aforementioned principles, if borne in mind in the early stages of the design and applied with ordinary ingenuity, will lead to reasonably satisfactory installations.

Considerations in Servomechanism Design

S. W. HERWALD
NONMEMBER AIEE

Synopsis: A servomechanism is in general any closed-cycle regulated system that is controlled by a difference of quantities. In this paper, methods are outlined by means of which the steady-state and transient performance of servomechanisms can be calculated. A general solution in operational form is given, and specific solutions are derived for a number of different types of angular-position servomechanisms. Among these is the solution for the Ward-Leonard type of control with RC antihunt feedback. This is probably the most common type of angular-position control.

SERVOMECHANISMS include many familiar regulator systems. The simple float valve controlling water level in a tank and the voltage regulator in reality are servomechanisms. The war has brought about many important new applications including: holding airplanes and ships on predetermined courses; stabilizing and rotating guns; producing exact duplicates of intricate patterns and dies; indicating flap or landing-gear position; and a multitude of others. Probably the most common servomechanism system is that which matches the position of a high-torque shaft to that of a low-torque shaft. This will be referred to as an angular-position servomechanism. By means of this servomechanism the high power required to operate a gun turret can be controlled so that the gun accurately matches the position of a low-power hand-operated sight. Whether the operating power is electric, hydraulic, or a combination of these or other types of power, the analysis to follow provides a basis for obtaining servomechanism performance in terms of basic design parameters.

Definition of a Servomechanism

In Figure 1, the nature of the component parts involved in a servomechanism and the meaning of the term closed-cycle are clearly illustrated. The three fundamental parts are the error measurer, the controller, and the output-quantity control member. As shown by the interconnecting arrows each of these basic parts is dependent for its operation upon one of the others and in turn influences the operation of the remaining part, hence the use of the term closed-cycle as shown by the closed counterclockwise system.

As previously defined, a servomechanism is in general any closed-cycle regulated system that is controlled by a difference of quantities. The wide scope of this definition can be realized best when it is noted that no limitation is placed on either the input or output quantities. Thus we may have regulation of speed, voltage, position, flow, and so forth. If the error measurer contains an element for converting quantities, the input and output quantities need not be the same. Many servomechanism problems have been solved commercially by Rototrol[11] or Amplidyne control or regulating systems. The basic component of each of these systems is rotating d-c equipment to which suitable control, either of the electronic or some other type, has been added.

The General Servomechanism

Figure 1 illustrates the basic elements of a general servomechanism and their interrelation. A theoretically perfect servomechanism would be one in which the output quantity varied with time exactly as the input. Referring to Figure 1 it is seen that this is not possible, since no error would be developed by which the output member could be controlled. The design of servomechanisms deals with

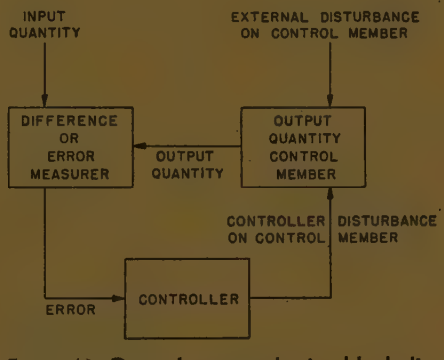


Figure 1. General servomechanism block diagram

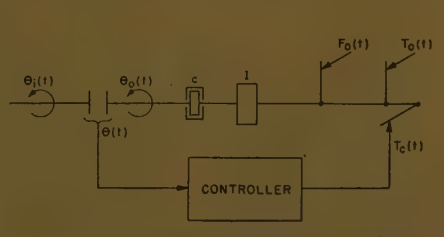


Figure 2. Angular-position servomechanism

methods of keeping both the magnitude and transient duration of the error between input and output small enough to meet the desired operating conditions.

The various elements in Figure 1, may be represented mathematically as time functions as follows:

$i(t)$ = input measurement as a function of time
$O(t)$ = output measurement as a function of time
$E(t)$ = error measurement as a function of time
$C(t)$ = control of output member as a function of time
$C_o(t)$ = external disturbance of output control member as a function of time

Also:

$$E(t) = i(t) - O(t) \tag{1}$$

The Laplacian operator form of any time function is given by

$$\phi(p) = p \int_0^\infty e^{-pt} \phi(t)\, dt \tag{2}$$

If $\phi(t)$, $\phi'(t)$, $\phi''(t)$, and so on $= 0$ at $t = o$, p may be thought of as equivalent to $\frac{d}{dt}$. When equation 2 is used, equation 1 becomes

$$E(p) = i(p) - O(p) \tag{3}$$

Also:

$$C(p) = f_c(p) E(p) \tag{4}$$

$$O(p) = f_o(p) [C(p) + C_o(p)] \tag{5}$$

where

$f_c(p)$ is the response function of the controller.
$f_o(p)$ is the response function of the output control member.

Both of the above quantities are similar to the response function of an electric network. In the familiar response of an LR series circuit, $\frac{1}{R+LP}$ corresponds to $f_c(p)$ of equation 4, and $C(p)$ and $E(p)$ are analogous to the current and impressed voltage, respectively.

By substituting equations 3 and 4 into equation 5 and solving for $E(p)$, one obtains

$$E(p) = \frac{i(p) - f_o(p)\, C_o(p)}{1 + f_o(p) f_c(p)} \tag{6}$$

This is the basic servomechanism equation and is of the type attributed to an unpublished memorandum of J. Taplin in 1937.[1] Taplin recognized the similarity of the automatic-control problem and the amplifier-design problem as given by Black[2] and Nyquist.[3] Integrating $E(p)$ in the Bromwich Wagner integral yields $E(t)$.

Over a period of years a number of solutions of equation 6 have been made. Certain assumptions and approximations

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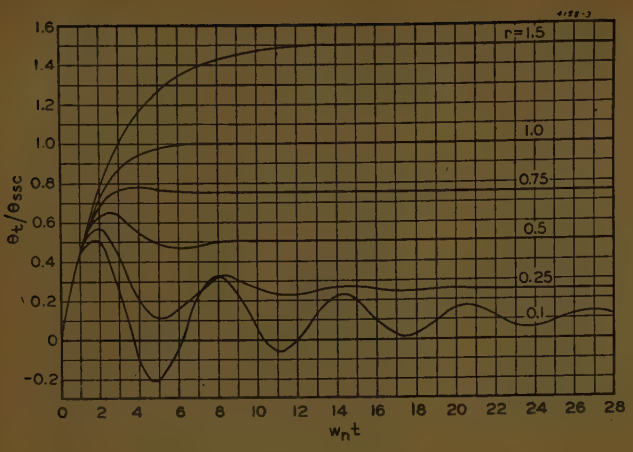


Figure 3. Response curves of simple-error servomechanism

were used to obtain these solutions. If these same assumptions and approximations can be justified by the designer, the established equations may be used, and the need for obtaining a new solution of equation 6 will have been eliminated. Once the characteristic equations of any given type of servomechanism have been obtained, they may be applied to any servomechanism of that type regardless of what the controlled parameter is. These equations permit analysis from which it is possible to design equipment to meet certain specifications. Stability as well as transient and steady-state errors can be predicted.

A good method to follow in the design of a servomechanism is to separate the mechanism into its component parts as shown by Figure 1, then to calculate the response functions of these components, and finally either to use an established solution of equation 6 such as those following or to obtain a new solution.

Angular-Position Servomechanism

The procedure to be followed in making an analysis of a servomechanism can be illustrated by consideration of the relatively common angular-position servomechanism. Such a system is schematically illustrated in Figure 2.
Where

$\theta_i(t)$ = input angle as a function of time
$\theta_o(t)$ = output angle as a function of time
$\theta(t)=\theta_i(t)-\theta_o(t)$ = error angle as a function of time
c = viscous damping coefficient of output member
I = lumped inertia of output member
$F_o(t)$ = coulomb friction as a function of time
$T_o(t)$ = external load torque as a function of time
$T_c(t)$ = controller torque as a function of time
$w_i(t)$ = input velocity as a function of time

The response of the output member can best be obtained by writing its differential equation:

$$\frac{Id^2\theta_o}{dt}+c\frac{d\theta_o}{dt}+T_o(t)+F_o(t)=T_c(t) \qquad (7)$$

At $t=0$ with $\frac{d\theta_o}{dt}$ and $o=0_a$ the Laplacian form of the foregoing solved for $\theta_o(p)$ is

$$\theta_o(p)=\frac{1}{Ip^2+cp}\,(T_c(p)-T_o(p)-F_o(p)) \qquad (8)$$

Comparing equation 8 and equation 5 one finds

$$f_o(p)=\frac{1}{Ip^2+cp}$$
$$C(p)=T_c(p)$$
$$C_o(p)=-T_o(p)-F_o(p)$$

By substituting into equation 6, one obtains

$$\theta(p)=\frac{(Ip^2+cp)\theta_i(p)+T_o(p)+F_o(p)}{Ip^2+cp+f_c(p)} \qquad (9)$$

Since $p\theta_i(p)=w_i(p)$, equation 9 can be written as

$$\theta(p)=\frac{(Ip+c)w_i(p)+T_o(p)+F_o(p)}{Ip^2+cp+f_c(p)} \qquad (10)$$

Thus once a control operator, $f_c(p)$, is determined, we have an error equation for either an angular-displacement or an angular-velocity input disturbance. Several solutions of equation 10 with different control operators, $f_c(p)$, are given.

Simple-Error Controller

The simplest form $f_c(p)$ can assume is a constant.

With $f_c(p)=K$ equation 4 becomes

$$T_c(p)=K\theta(p) \qquad (11)$$

This gives a controller whose torque is proportional to the error angle, and K is known as the stiffness constant. If $f_c(p)=K$, $T_o(p)=F_o(p)=0$, and $w_i(p)$ is made w_1 at $t=0$, equation 10 becomes

$$\theta(p)=\frac{(Ip+c)w}{Ip^2+cp+K} \qquad (12)$$

The effects of $T_o(p)$ and $F_o(p)\neq 0$ can be obtained by superposition.
Setting $p=0$ in equation 12 gives the steady-state error

$$\theta_{ss}=\frac{cw_1}{K} \qquad (13)$$

With the effects of $T_o(t)$ and $F_o(t)$ considered

$$O_{ss}=\frac{cw_1+T_o+F_o}{K} \qquad (14)$$

Similarly in equation 9 with $\theta_i(p)=\theta_1$

$$\theta_{ss}=\frac{T_o+F_o}{K} \qquad (15)$$

In determining stability and transient response of a servomechanism, it is convenient to use some standard input function and then to compare the errors obtained using different controller parameters. A very useful and practical input function of that type is constant velocity, w_1, applied to the input at $t=0$ and held at that value thereafter. This is the case described by equation 12 for the simple-error type of controller. Rewriting equation 12 in terms of $r=\frac{c}{c_c}=\frac{c}{2\sqrt{IK}}$, the ratio of actual to critical damping, and $w_n=\sqrt{\frac{K}{I}}$ the undamped natural frequency of the system yields a more general solution. Then

$$\theta(p)=\frac{(p+2rw_n)w_1}{p+2rw_np+w_n^2} \qquad (16)$$

The solutions for equation 16 obtained by operational methods are

$r<1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{-rw_nt}\left(\cos\sqrt{1-r^2}\,w_nt+\frac{2r^2-1}{2r\sqrt{1-r^2}}\sin\sqrt{1-r^2}w_nt\right)\right) \qquad (17)$$

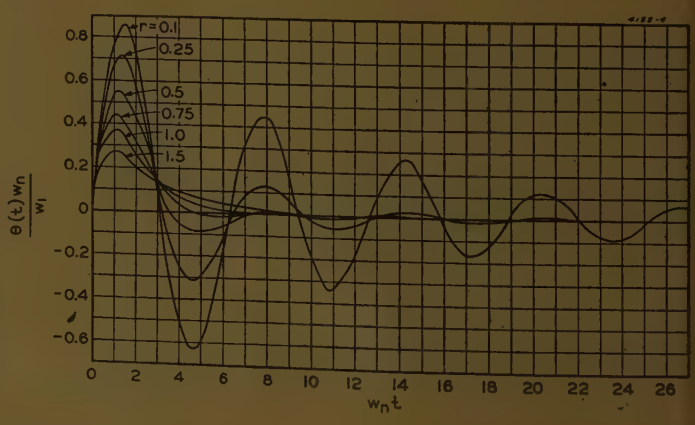


Figure 4. Response curves of error-plus-error-rates servomechanism with no viscous damping and (l-a) held constant

$r=1$

$$\frac{\theta(t)}{\theta_{ssc}}=1-e^{-w_nt}\left(1+\frac{w_nt}{2}\right) \quad (18)$$

$r>1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{rw_nt}\left(\cosh\sqrt{r^2-1}w_nt+\frac{2r^2-1}{2r\sqrt{r^2-1}}\sinh\sqrt{r^2-1}w_nt\right)\right) \quad (19)$$

θ_{ssc} is the steady-state error with critical damping and is equal to $2w_1/w_n$. $\theta_{ssc}r=2rw_1/w_n$ and checks θ_{ss} in equation 13. Figure 3 gives $\theta(t)/\theta_{ssc}$ as the ordinate and w_nt as the abscissa, for different values of r. Since both $\theta(t)/\theta_{ssc}$ and w_n are dimensionless and neither contains r, the curves shown in Figure 3 give both the relative magnitude and duration of the transient errors. Figure 3 is similar to the curves given by Hazen,[4] with the exception that different dimensionless parameters which give a truer picture of transient duration were used. As expected Figure 3 shows that when the damping ratio, r, is increased, the oscillation about the steady-state position is reduced, although the time initially to approach the steady-state error is increased. If a fairly large initial overshoot can be tolerated, small damping ratios may give satisfactory performance because of their correspondingly lower steady-state errors.

The prime difficulty with a regulator of this type occurs because the damping torque is proportional to speed. Consequently, at high operating speeds, there is a large amount of power being dissipated in the damper. This loss can be tolerated in low-power systems; however, high-power systems require other means of obtaining stable operation. As is usual in design, compromise is necessary. High values of r are desired to prevent large transient overshoot, yet these same values bring about an increase in the steady-state error and damping power loss.

Error-Plus-Error-Rates Controller

Examination of equation 10 shows that to overcome the disadvantages of the simple-error controller a positive term in p and a negative term in p^2 are desired in the denominator. The positive term will provide damping to the system without increasing the steady-state error or damping loss for a constant-velocity input,

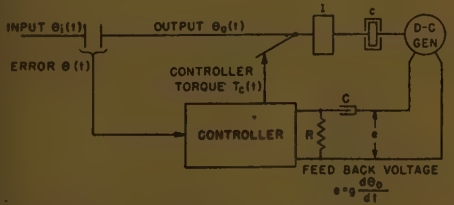


Figure 5 Schematic representation of error-plus-*RC*-output-feedback servomechanism

since both are dependent only on c. The negative term in p^2 will minimize inertia effects. Such a controller would give $f(p)$ in equation 10 the form,

$$f_c(p)=-ap^2+bp+K \quad (20)$$

and

$$T_c(t)=-a\frac{d^2\theta}{dt^2}+b\frac{d\theta}{dt}+K\theta \quad (21)$$

If equation 1 and equation 7 are used, the differential torque equation can be written as

$$I\frac{d^2\theta_i}{dt^2}+c\frac{d\theta_i}{dt}+T_o(t)+F_o(t)=(I-a)\frac{d^2\theta}{dt}+(b+c)\frac{d\theta}{dt}+K\theta \quad (22)$$

Rewritten in operational form

$$\theta(p)=\frac{(mp^2+2frw_np)\theta_i(p)+(1-m)pw_1+T_o(p)+F_o(p)}{p^2+2rw_np+w_n^2} \quad (23)$$

with the initial boundary values, $(1-m)pw_1$ showing up as an external torque and where

$$m=\frac{I}{I-a},\quad f=\frac{c}{b+c},\quad r=\frac{b+c}{2\sqrt{(I-a)K}},$$

$$w_n=\sqrt{\frac{K}{I-a}}$$

With $p\theta_i(p)=w_1$ and $T_o(p)=F_o(p)=0$ equation 23 becomes

$$\theta(p)=\frac{(p+2frw_n)w_1}{p^2+2rw_np+w_n^2} \quad (24)$$

Solutions by operational methods give

$r<1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{-rw_nt}\left(\cos\sqrt{1-r^2}w_nt+\frac{2fr^2-1}{2fr\sqrt{1-r^2}}\sin\sqrt{1-r^2}w_nt\right)\right) \quad (25)$$

$r=1$

$$\frac{\theta(t)}{\theta_{ssc}}=1-e^{-w_nt}\left(1+w_nt-\frac{w_nt}{2f}\right) \quad (26)$$

$r>1$

$$\frac{\theta(t)}{\theta_{ssc}}=r\left(1-e^{-rw_nt}\left(\cosh\sqrt{r^2-1}w_nt+\frac{2fr^2-1}{2fr\sqrt{r^2-1}}\sinh\sqrt{r^2-1}w_nt\right)\right) \quad (27)$$

Where

$$\theta_{ssc}=\frac{2fw_1}{w_n}$$

Most designers strive to make c as small as possible so that steady-state error for constant velocity input is minimized. The solutions of equation 24 with $c=f=0$ are

$r<1$

$$\frac{\theta(t)w_n}{w_1}=\frac{e^{-rw_nt}}{\sqrt{1-r^2}}\sin\sqrt{1-r^2}w_nt \quad (28)$$

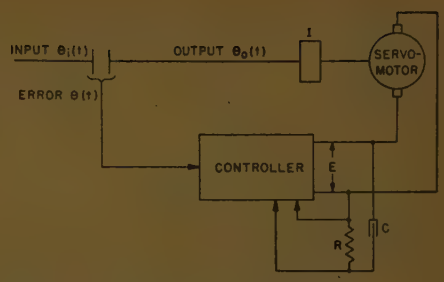


Figure 6. Schematic representation of error-plus-negative-*RC*-feedback servomechanism

$r=1$

$$\frac{\theta(t)w_n}{w_1}=w_nte^{-w_nt} \quad (29)$$

$r>1$

$$\frac{\theta(t)w_n}{w_1}=\frac{e^{-rw_nt}}{\sqrt{r^2-1}}\sinh\sqrt{r^2-1}w_nt \quad (30)$$

The foregoing equations are particularly useful because they result in simple transient solutions to which the steady-state error for $f\to 0$ can be added without any appreciable inaccuracy. Figure 4 is a plot of $\frac{\theta(t)w_n}{w_1}$ against w_nt for different values of r with $(I-a)$ held constant. It should be noted carefully that for stability $a<I$, since the first criterion of stability is that all coefficients be positive. Again as in Figure 3, the abscissa, w_nt, does not contain r so that it truly represents transient durations for variations of r. Figure 4 illustrates quite clearly the compromise involved in derivative damping:

(*a*). Transient duration may be decreased at the expense of peak error.

(*b*). Transient peaks may be decreased by providing a controller capable of delivering high values of derivative damping. This usually involves a more elaborate controller.

As difficult as it is to obtain high value of first derivative damping reliably, the problem is extremely more difficult in obtaining a component proportional to p^2. Usually one takes the values of a that can be obtained and has little control over its magnitude.

Error Controller With RC-Output Feedback

The difficulty of obtaining economically quantities accurately proportional to error change has led to a wide practice of using a resistor–capacitor network across a voltage proportional to the output speed, $d\theta_o/dt$, to obtain antihunt. A common system is to use a d-c tachometer generator directly connected to the output drive. A schematic diagram of a system of this type is shown in Figure 5. Neglecting load and friction torque, torques may be equated as follows:

$$I\frac{d^2\theta_o}{dt^2}+c\frac{d\theta_o}{dt}=T_c(t) \quad (31)$$

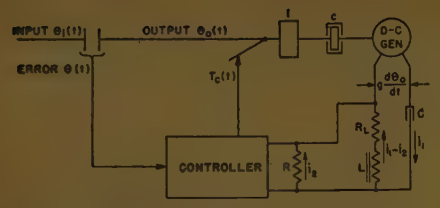


Figure 7. Schematic representation of error-plus-*LRC*-output-feedback servomechanism

Two sets of initial conditions have to be assumed, because different transient responses are obtained when the output velocity function $p\theta_i(p)$ is taken as an acceleration or deceleration. This is caused by the charge on the capacitor, C, for a steady-state velocity The two sets of conditions assumed are:

A. At $t=0$

$$\theta=\theta_i=\theta_o=0$$

$$\frac{d\theta}{dt}=\frac{d\theta_i}{dt}=\frac{d\theta_o}{dt}=0$$

$$\left(\frac{1}{C}\int i\,dt\right)_{t=0}=0 \qquad (32)$$

B. At $t=0$

$$\frac{d\theta}{dt}=\theta_o=0$$

$$\theta_i=\theta=\frac{cw_1}{K}$$

$$\frac{d\theta_i}{dt}=\frac{d\theta_o}{dt}=w_1$$

$$\left(\frac{1}{C}\int i\,dt\right)_{t=0}=gw_1 \qquad (33)$$

The solutions for $\theta(p)$ using boundary conditions (equations 32 and 33) are identical with the exception that the initial values given by equation 33 show up as an external torque. $C_0(p)$ in equation 6. Thus as derived in equations 56 and 60.

$$O(p)=\theta_o(p)$$

$$f_o(p)=\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}}$$

$$C(p)=K\theta(p) \qquad (34)$$

$$E(p)=\theta(p)$$

$$f_c(p)=K$$

$C_o(p)=0$ using equation 32

$$C_o(p)=\left(I+\frac{K_2RCg}{1+RCp}\right)pw_1 \text{ using equation 33}$$

The solution for $\theta(p)$, using equation 32, is given by equation 57 as

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}\right)\theta_i}{Ip+cp+\dfrac{K_2RCgp^2}{1+RCp}+K}(p) \qquad (35)$$

Letting $p\theta_i(p)=w_1$ at $t=0$

$$\theta(p)=\frac{\left(p^2+hp+\dfrac{c}{RCI}\right)w_1}{p^3+hp^2+\left(\dfrac{c}{RCI}+\dfrac{K}{I}\right)p+\dfrac{K}{RCI}} \qquad (36)$$

where

g = generator constant, volts per radian per second

K_2 = controller constant, foot-pound per volt

$$h=\frac{1}{RC}+\frac{K_2g}{I}+\frac{c}{I}$$

The corresponding solution for $\theta(p)$ using equation 33 is given by equation 61 as

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}\right)\theta_i(p)-\left(I+\dfrac{K_2RCg}{1+RCp}\right)pw_1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}+K} \qquad (37)$$

If at $t=0$, $p\theta_i(p)$ is instantaneously made zero, $\theta_i(p)$ remains at the value cw_1/K and

$$\theta(p)=\frac{\left(p^3+hp^2+\dfrac{c}{RCI}p\right)\dfrac{cw_1}{K}-\left(p+h-\dfrac{c}{I}\right)pw_1}{p^3+hp^2+\left(\dfrac{c}{RCI}+\dfrac{K}{I}\right)p+\dfrac{K}{RCI}} \qquad (38)$$

The steady-state error for constant velocity input, w_1, is still cw_1/K as it was for the simple-error case. To reduce this error, c is made as small as possible. The case with $c=0$ is of importance and reduces both equations 36 and 38 to the same value with the exception of sign. With the positive sign

$$\theta(p)=\frac{\left(p+\dfrac{1}{RC}+\dfrac{K_2g}{I}\right)pw_1}{p^3+\left(\dfrac{1}{RC}+\dfrac{K_2g}{I}\right)p^2+\dfrac{K}{I}p+\dfrac{K}{RCI}} \qquad (39)$$

For $\theta(t)$ to be damped the product of the second and third denominator term coefficients must exceed the first and fourth term coefficient product. This

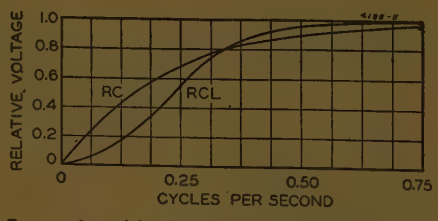


Figure 8. Absolute voltage comparison of *RC* and *LRC* antihunt circuits

R=200,000 R_L=100,000
C=3 microfarads L=20,000 henrys

requires $K_2RCg>0$ which is a result that easily could have been anticipated. Solutions for $\theta(t)$ from equations 36, 38 and 39 may be obtained by general methods. However, in many cases, it is easier to assign specific values to the constants, find the roots of the equation by a method with which one is most familiar, and obtain several particular solutions by operational methods, rather than a general solution. If c arbitrarily is made zero only in the denominator of equations 36 and 38, equations result which contain a steady-state velocity error yet do not derive any damping from that energy loss. This represents the case of a d-c motor with thyratron armature control. Here we have a velocity error, because a definite armature voltage must be maintained for constant velocity, and yet no decelerating damping because of the rectifier characteristic of the thyratrons.[5,6]

The big disadvantage of using *RC* output damping is that it retards acceleration response.

$\theta(p)$ for Error Controller With Negative RC Feedback

Typical of the system outlined in Figure 6, is a Ward-Leonard control system with the generator included in the controller. E of Figure 6 then corresponds to the variable voltage of the Ward-Leonard system. The well-known equation for armature current in a d-c motor with constant excitation is

$$i_a=\frac{E-g\dfrac{d\theta_o}{dt}}{Ra}$$

With load torque neglected the accelerating torque is proportional to i_a or

$$C_1i_a=\frac{EC_1-gC_1\dfrac{d\theta_o}{dt}}{Ra}=I\frac{d^2\theta_o}{dt^2} \qquad (40)$$

By following the procedure outlined in the Appendix, and using conditions in equations 32 and 33 again, one finds

$$f_o(p)=\frac{1}{Ip^2+cp}$$

$$C(p)=\frac{K(1+RCp)\theta(p)}{1+RC(1+K_2)p} \qquad (41)$$

$$f_c(p)=\frac{K(1+RCp)}{1+RC(1+K_2)p}$$

$C_o(p)=0$ using equation 32

$$C_o(p)=\left(I+\frac{cK_2RC}{1+RC(1+K_2)p}\right)pw_1 \text{ using equation 33}$$

Use of conditions in equation 32, substitution in equation 6 yields

$$\theta(p)=\frac{(Ip^2+cp)\theta_i(p)}{Ip^2+cp+\dfrac{K(1+RCp)}{1+RC(1+K_2)p}}$$

Letting $p\theta_i(p)=w_1$ at $t=0$

$$\theta(p)=\frac{\left(p^2+\left(h_1+\dfrac{c}{I}\right)p+\dfrac{h_1c}{I}\right)w_1}{p^3+\left(h_1+\dfrac{c}{I}\right)p^2+\left(\dfrac{RCK}{I}+\dfrac{c}{I}\right)h_1p+\dfrac{Kh_1}{I}} \qquad (42)$$

where

$$h_1=\frac{1}{RC(1+K_2)}$$

Use of conditions in equation 33 and substitution in equation 6 yield

$$\theta(p)=\frac{(Ip^2+cp)\theta_i(p)-\left(I+\frac{(cK_2RC)}{1+RC(1+K_2)p}\right)pw_1}{Ip^2+cp+\frac{K(1+RCp)}{1+RC(1+K_2)p}} \quad (43)$$

If at $t=0$, $p\theta_i(p)$ is instantaneously made zero, $\theta_i(p)$ remains at the value $\frac{cw_1}{K}$ and

$$\theta(p)=\frac{\left[\left(p^2+\left(h_1+\frac{c}{I}\right)p^2+\frac{h_1cp}{I}\right)\frac{cw_1}{K}-\left(p+h_1+\frac{cK_2}{I(1+K_2)}\right)pw_1\right]}{p^3+\left(h_1+\frac{c}{I}\right)p^2+\left(\frac{RCK}{I}+\frac{c}{I}\right)h_1p+\frac{Kh_1}{I}} \quad (44)$$

As was the case with output damping, $\theta(t)$ can be found by operational methods. This system of obtaining stability also is detrimental to acceleration response because it depends for its operation on slowing up the appearance of the voltage, E.

Error Controller With LRC Output Feedback

With a feedback circuit such as used in Figure 7, rather than one as used in Figure 5, the advantage of a faster acceleration response without sacrifice of antihunt is obtained. The simplest way to show what results is to plot the absolute value of the voltage appearing across R against frequency for both the RC and LRC cases. Figure 8 gives such a comparison. In the lower-frequency range the LRC circuit gives less negative feedback, consequently, acceleration rates are higher. However, in the higher-frequency ranges the negative feedback of both the LRC and RC circuits are about the same. By selecting the value of L, R, and C the frequency at which the feedback voltage first reaches its peak can be made the natural hunting frequency of the system. If this be done, the system is damped because of high negative feedback at hunting frequency, and yet acceleration response is not too badly affected because the amount of negative feedback falls off rapidly at frequencies below the hunting frequency. Using an inductance in this manner is quite similar to the method used to increase the gain of a capacitor-coupled amplifier at high frequencies. Figure 7 shows only one section of this type of high-pass filter. More than one section gives even a sharper change from low to high negative-feedback values than that shown in Figure 8. The solution of the LRC problem in operational form is obtained by following the same procedure outlined in the appendix. Then

$$f_o(p)=\frac{1}{Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R+R_L+Lp+RCp(R_L+Lp)}} \quad (45)$$

$$C(p)=K\theta(p)$$
$$f_c(p)=K$$
$$C_o(p)=0 \text{ (using equation 32)}$$

$$C(p)=\left(I+\frac{K_2RCgR_L+Lp}{R+R_L+Lp+RCp(R_L+Lp)}\right)pw_1$$

When equations 32 and 45 are substituted into equation 6 and one lets $p_i\theta(p)=w_1$ at $t=0$

$$\theta(p)=\frac{\left(Ip+c+\frac{K_2RCg(R_L+Lp)p}{R+R_L+Lp+RCp(R_L+Lp)}\right)w_1}{Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R+R_L+Lp+RCp(R_L+Lp)}+K} \quad (46)$$

When equations 33 and 45 are used similarly and substituted into equation 6, then if at $t=0$ w_1 instantaneously is made zero, $\theta(p)$ remains at cw_1/K and

$$\theta(p)=\frac{\left(Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R_L+R+Lp+RCp(R_L+Lp)}\right)\frac{cw_1}{K}-\left(I+\frac{K_2RCg(R_L+Lp)}{R+R_L+Lp+RCp(R_L+Lp)}\right)pw_1}{Ip^2+cp+\frac{K_2RCg(R_L+Lp)p^2}{R_L+R+Lp+RCp(R_L+Lp)}+K} \quad (47)$$

Time Delays

All the equations given so far have neglected time delays. In systems in which this delay time is small compared to the natural period, the equations can be used as given. However, when long relative delay times are involved, they must be introduced. In the familiar case of current response of a series LR circuit,

$$i=\frac{1}{1+\frac{L}{R}p}\cdot\frac{E}{R}$$

and L/R occurring in the denominator can be thought of as a time delay, t. The steady-state value, E/R, has already been included in the constants K and K_2 used in the response equations. Therefore, in order to introduce time delay the affected term merely should be multiplied by $\frac{1}{1+tp}$. If more than one time delay is to be included, by a similar line of reasoning the affected term is multiplied by

$$\frac{1}{(1+t_1p)(1+t_2p)\ldots(1+t_np)} \quad (48)$$

Thus for the Ward-Leonard system which contains both field and armature time delays, equation 35 becomes

$$\theta(p)=\frac{\left(Ip^2+cp+\frac{K_2RCgp^2}{(1+t_fp)(1+t_ap)(1+RCp)}\right)\theta_i(p)}{Ip^2+cp+\frac{K_2RCgp^2}{(1+t_fp)(1+t_ap)(1+RCp)}+\frac{K}{(1+t_fp)(1+t_ap)}}$$

Similarly equations including time delay could be written for all the response equations given. Rationalizing an equation of the type given in equation 49 results in a denominator containing a fifth degree equation in p. Finding the roots of that equation and then using operational methods gives the solution for $\theta(t)$. The increasing complexity of the solution for $\theta(p)$, when several time delays are included, has resulted in a practice of finding the roots of the denominator equation and evaluating the stability by the damping factors of the roots.[7,8]

Types of Input Functions

All the response equations given have used a unit function as the input. This is a simple function by which the response for the different systems of control can be directly compared. However, it is not the only one that can be used. If the response to a sine, constant acceleration, or any other input is desired, it is necessary merely to substitute the correct operational form of the sine or constant acceleration input for $\theta_i(p)$ and proceed with the solution exactly as if w_1 had been substituted for $p\theta_i(p)$. This provides a method for finding the exact response to a known input function. Most problems, however, involve random inputs, consequently a good method of comparison such as is provided by the unit function is adequate.

Summary

The previous example of a remotely located gun turret continuously following a hand-operated sight serves well to illustrate the method and equations given. In the design of such a system it is desired that the gun stay in line with the sight within close limits. It is also desired that the gun deviate as little as possible and realign itself as quickly as possible after some disturbance has moved the sight. Thus, the sight position corresponds to $\theta_i(t)$ and the difference in their respective positions at any time to $\theta(t)$. Finding $\theta(t)$ helps enormously in designing a control system, for not only is it

the error equation but also it shows what values such fundamental parameters as the stiffness of the system, K, the viscous damping, c, the inertia, I, and the antihunt feedback constant, K_2, must have in order to keep errors within desired limits. The error equation in operational form $\theta(p)$, has been given for a number of different systems of control, any of which will do the job. Each system accomplishes the basic purpose of keeping the gun turret aligned with the sight. However, the transient and steady-state errors may be quite different for similar values of stiffness, damping, inertia, and so forth. It is the problem of the designer to evaluate the complexity of any system against its merit and then select the simplest system that will perform within desired limits. Setting p equal to zero in any of the derived equations for $\theta(p)$ gives steady-state errors for angular displacement and velocity. From these can be selected values of viscous damping and stiffness which will give a small enough steady-state error to meet a given specification. Integrating $\theta(p)$ operationally gives $\theta(t)$, the transient error response. From the resulting equation values of antihunt, K_2, or error rate of change, b, can be selected which will limit the peak error to the desired value.

The method shown for obtaining these criterion equations for $\theta(p)$, consists of breaking the servomechanism up into its component parts, obtaining the controller and output response functions, and then obtaining $\theta(p)$ by using the general servomechanism equation 6. This is a systematic procedure for handling an unwieldy problem. Further refinement can be made by introducing time delays and varied input functions. Although all particular solutions obtained by use of equation 6 were for angular position regulators, equation 6 is entirely general. Therefore servomechanisms to control such quantities as voltage speed, pressure, and so forth are represented by equation 6. Particular solutions obtained for the angular position servomechanism can be applied to servomechanisms controlling other quantities by using unit analogy.

Appendix. $\theta(t)$ for Error Controller With *RC* Output Feedback

In order to calculate the feedback component of torque, it is first necessary to find the voltage appearing across R of Figure 5, as a function of time. The series voltage summation is

$$iR+\frac{1}{C}\int idt=g\frac{d\theta_0}{dt} \tag{49}$$

By standard operational methods this can be rewritten as

$$Ri(p)+\frac{i(p)}{Cp}+\left(\frac{1}{C}\int idt\right)_{t=0}= gp\theta_0(p)-gp(\theta_0)_{t=0} \tag{50}$$

When conditions from equation 32 are used, the voltage appearing across R, is

$$Ri(p)=\frac{RCgp^2\theta(p)}{1+RCp}$$

which, when fed back into the controller negatively, gives an output torque component

$$-K_2Ri(p)=-\frac{K_2RCgp^2\theta_o(p)}{1+RCp} \tag{51}$$

where K_2 is the controller constant with units of foot-pounds per volt. Then for a controller which also has a simple error component, the controller torque is

$$Tc(p)=K\theta(p)-\frac{K_2RCgp^2\theta(p)}{1+RCp} \tag{52}$$

With load, $T_o(t)$, and friction, $F_o(t)$, neglected, the differential torque equation is

$$I\frac{d^2\theta_o}{dt^2}+c\frac{d\theta_o}{dt}=T_c^*(t) \tag{53}$$

When operational methods and boundary conditions from equation 32 are used

$$Ip^2\theta_o(p)+cp\theta_o(p)=Tc(p)=K\theta(p)- \frac{K_2RCgp^2\theta_o(p)}{1+RCp} \tag{54}$$

This can be rewritten in the form,

$$\theta_o(p)=\left(\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}}\right)K\theta(p) \tag{55}$$

When equations 4 and 5 are used, the following comparisons can be made:

$$O(p)=\theta_o(p)$$

$$f_o(p)=\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}} \tag{56}$$

$$C(p=K\theta(p)$$
$$E(p)=\theta(p)$$
$$f_c(p)=K$$

By using equations 6 and 56 and rationalizing, one obtains

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}\right)_i(p)}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}+K} \tag{57}$$

If the same procedure previously outlined is followed and conditions from equation 33 are used,

$$T_c(p)=K\theta(p)-\frac{K_2RCg(p^2\theta_o(p)-pw_1)}{1+RCp} \tag{58}$$

Use equation 56, boundary conditions of equation 33 and solve for $\theta(p)$:

$$\theta_o(p)=\left(\frac{1}{Ip^2+cp+\dfrac{K_2RCgp^2}{1+RCp}}\right)\times \left[K\theta_o(p)+\left(I+\frac{K_2RCg}{1+RCp}\right)pw_1\right] \tag{59}$$

Comparison with equation 5 gives

$$C_o(p)=\left(I+\frac{K_2RCg}{1+RCp}\right)pw_1 \tag{60}$$

in addition to the previous relations, which were established in equation 56. Thus the initial boundary conditions occur as external torques in the general equation. By using equations 6, 56, and 60 and rationalizing, we obtain

$$\theta(p)=\frac{\left(Ip^2+cp+\dfrac{K^2RCgp^2}{1+RCp}\right)\theta_i(p)- \left(I+\dfrac{K_2RCg}{1+RCp}\right)pw_1}{Ip^2+cp+\dfrac{K^2RCgp^2}{1+RCp}+K} \tag{61}$$

References

1. Transients in Linear Systems (book), M. F. Gardner, J. L. Barnes. John Wiley and Sons, Inc., New York, N. Y., 1942. Volume 1, page 193.

2. Stabilized Feedback Amplifiers, H. S. Black. *Bell System Technical Journal*, January 1934, pages 1–18.

3. Regeneration Theory, H. Nyquist. *Bell System Technical Journal*, January 1932, pages 126–47.

4. Theory of Servomechanisms, H. L. Hazen. *Journal* of Franklin Institute, volume 218, September 1934, pages 279–331.

5. Thyratron Motor Control, E. E. Moyer, H. L. Palmer. AIEE Transactions, volume 62, 1943, November section, pages 706–12.

6. Theory of Rectifier—D-C Motor Drive. E. H. Vedder, K. P. Puchlowski. AIEE Transactions, volume 62, 1943, pages 863–70.

7. Recent Developments in Generator Voltage Regulation, C. R. Hanna, K. A. Oplinger, C. E. Valentine. AIEE Transactions, volume 58, 1939, pages 838–44.

8. Recent Developments in Speed Regulation, C. R. Hanna, K. A. Oplinger, S. J. Mikina. AIEE Transactions, volume 59, 1940, pages 692–700.

9. Complex Variables and Operational Calculus (book), N. W. McLachlan. Macmillan Company, New York, N. Y., 1942.

10. Operational Circuit Analysis (book), V. Bush. John Wiley and Sons, Inc., New York, N. Y., 1929.

11. Rototrol—A Versatile Electrical Regulator, W. H. Formhals. *Westinghouse Engineer*, May 1942, page 51.

Design Considerations of 400-Cycle Aircraft Motors

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SEVERAL papers have been presented describing the reason for the selection of 400-cycle three-phase service for motors aboard aircraft. In view of the fact that the decision has been made to use 400 cycles, the purpose of this paper is to present some of the motor-design problems to the men who must write the specifications for these motors. A better general understanding of the performance to be expected from 400-cycle induction motors will help both the aircraft electrical engineer and the motor designer.

An electric system such as used on an airplane must be designed as a unit. The generating system is never sufficiently large to permit the assumption of uniform voltage and frequency under all conditions. The need for dependability requires the use of some reliable overload-protection device which must be considered as part of the motor. The type of load, which may involve a large variation in torque during the duty cycle, must be completely understood by the motor designer. Also, the motor control must be such as to insure reliable motor operation. The need for co-operation between the engineers working on these varied but intimately related problems cannot be too highly emphasized.

Only those characteristics of 400-cycle motors which differ radically from characteristics of the 24-volt d-c motors used in the past will be discussed. Mechanical problems will not be discussed, although new mechanical problems have appeared due to the possibility of speeds up to 24,000 rpm.

First we will list the requirements of the airplane designer and then point out some of the problems faced by the motor designer to meet these requirements.

1. The first requirement of the airplane designer, so obvious that it is frequently overlooked or subordinated to other requirements, is dependability. The motor must do the job assigned to it unfalteringly on each occasion when it is needed during the period between servicing. In this respect the induction motor has definite obvious advantages over the 24-volt d-c motor. These include simpler windings, freedom from commutation, and fewer parts.

2. The weight must be minimum consistent with dependability.

3. The motor must be capable of doing its job when the maximum voltage is ten per cent above or ten per cent below rating, and when the frequency is five per cent above or below rating.

4. The requirements for starting torque in proportion to full-load torque will vary considerably, but should ordinarily not be below 120 per cent of full-load torque.

5. The maximum torque permissible is determined by the airplane structure and is sometimes as low as 120 per cent of the full-load torque.

6. If the motor is to run continuously, its efficiency may be as important as its weight because of the added weight of fuel which must be carried to run a less efficient motor.

7. It is desirable to have a high power factor to reduce line losses and generating capacity required.

8. The same ambient temperature, humidity, altitude, and vibration specifications apply to 400-cycle motors as were used with the d-c motors.

These, then, are the primary requirements of the airplane designer which affect the motor designer. The generalization that high speed means less weight per horsepower will now be investigated by considering the results of running a familiar one-horsepower 60-cycle four-pole induction motor from a 400-cycle power supply.

The motor selected is one having quite common performance characteristics. The starting torque is 200 per cent of full-load torque, and the maximum or breakdown torque is 300 per cent of full-load torque. The efficiency is 80 per cent and the slip at full load is four per cent. The motor being considered differs from an ordinary industrial motor in that better materials are used and precautions are taken to prevent interlamination losses. The motor is totally enclosed so that the analysis will not be confused too much by increased friction and windage. Otherwise, the motor is a conventional one with a well proportioned lamination and normal performance.

The full-load torque of any motor is the maximum torque which can be developed by the motor for the rated time of operation without exceeding a safe operating temperature. This is the case in all motors, although the interpretation of "safe operating temperature" will be revised drastically when the motor is used aboard an airplane. For purposes of comparison let us assume that the same cooling is used on 400 cycles as on 60 cycles, that the same temperature rise is permitted, and therefore the same total losses in the motor are permissible.

Let us now consider how this motor will operate on 400-cycle service. In order to keep the flux the same, the motor must be operated on a voltage 6.7 times higher than the 60-cycle voltage. That is, the voltage must be increased in proportion to frequency. The synchronous speed, of course, increases from 1,800 rpm to 12,000 rpm. The magnetizing current increases only slightly. The motor resistance is not changed at all by operating the motor on 400 cycles; however, the leakage reactance increases in proportion to the frequency, that is 6.7 times.

The rated torque is determined by the losses, and is practically the same as on 60 cycles. Therefore, the horsepower rating is increased by 6.7 times. The breakdown torque has increased by 70 per cent to over 500 per cent of full-load torque. This follows from the increase of voltage proportional to frequency, together with a much smaller increase in circuit impedance.

Since the motor produces the same full-load torque with the same losses, but at 6.7 times the speed, the efficiency has been increased considerably. The efficiency has come up from 80 per cent to 95 per cent or better.

The slip at full-load torque is reduced from four per cent to 0.6 per cent. This can be understood by picturing what happens from the standpoint of the rotor. The torque is produced by the action of the rotor current on the rotating flux produced by the field. By operating the motor on 400 cycles instead of 60 cycles, the magnitude of the flux is not altered; only the speed of the rotating flux has been changed. On 60 cycles, with a slip of four per cent or 72 rpm, the rotor bars were cutting the flux at such a speed as to produce sufficient rotor current to develop full-load torque. On 400 cycles the flux is rotating 12,000 rpm; however, a difference in speed of 72 rpm between the rotating field and the rotor still produces sufficient rotor current to develop full-load torque. The current in the rotor under load depends on the voltage induced in the bars, which in turn depends on the rate of cutting flux.

The slip at which maximum torque is obtained will also be very small. Maximum torque on 60 cycles is obtained at 30 per cent slip, the slip for maximum torque on 400 cycles will be near five per cent. The fact that all torques from zero to maximum are produced within a range of five per cent is useful in synchronizing actuators located at remote parts of the airplane.

So far we have presented a very satis-

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factory picture of increased performance in horsepower per pound by increasing the frequency. One disturbing factor remains to be considered. This factor is the starting torque. A 60-cycle motor designed for maximum horsepower per pound has adequate starting torque. However, a 400-cycle motor designed for maximum efficiency and horsepower per pound has a relatively small starting torque. Redesign by changing the circuit constants of the 400-cycle motor results in a loss of efficiency and consequent increase in weight per horsepower.

The starting torque is proportional to the I^2R loss in the rotor divided by the synchronous speed. Returning to the example, the locked rotor current on 400 cycles is 40 per cent higher than the current on 60 cycles. The I^2R loss will approximately double, while the synchronous speed is increased 6.7 times; the torque, then, will be reduced 3.3 times. This means that instead of 200 per cent starting torque the motor now develops 60 per cent starting torque. In actual practice the rated full-load torque of the motor could be increased considerably by inproving the cooling and by raising the allowable temperature limit. This increase in rating only serves to lower the percentage starting torque. This difficulty is a major problem in the design of 400-cycle motors for aircraft use.

This example has been purposely selected to show the difficulty clearly. This difficulty is not nearly so pronounced in very small motors because the leakage reactance is relatively small in comparison to the resistance, and the majority of motors used aboard aircraft are small. The problem remains in some degree, however, and must be overcome. Figure 1 illustrates the example. Speeds in percentage of synchronous are plotted as ordinates and torques are plotted as abscissas. The motor on 400 cycles produces the same torque at rated horsepower as on 60 cycles, but produces much less torque at standstill.

We shall now consider ways of increasing this usually inadequate starting torque. As previously stated, the starting torque varies directly as the I^2R loss in the rotor and inversely as the synchronous speed. To increase the torque, the rotor current or the rotor resistance, or both, must be increased. Table I shows the comparative values obtained in operating our one-horsepower motor on 60 cycles and

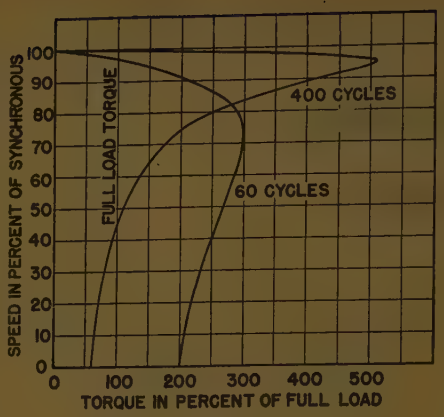


Figure 1. Speed–torque curves

on 400 cycles, together with the effect of increasing the rotor resistance. In many aircraft applications it may be desirable to produce the high maximum torque at standstill. In the example chosen this would be accomplished by increasing the rotor resistance about 20 times. The penalty, however, would lie in the necessary decrease in rated torque due to the heating in the high-resistance rotor. The reduction in rated torque would be by a factor of approximately three. With the starting torque 500 per cent of the motor's previous rating, and the rating reduced by a factor of three, the starting torque would now be about 1,500 per cent of rated.

Although this large starting torque is obtainable, a reasonable value of 200 per cent starting torque can be obtained by increasing the rotor resistance without much sacrifice in rated torque. In the example chosen, a threefold increase in rotor resistance will achieve this result with a net decrease in rated torque of 20 per cent.

A second method of increasing the starting torque is by raising the starting current by decreasing the reactance. The reactance is a function of slot size and proportions, and air gap. Let us assume that the reactance could be cut in half. This would approximately double the starting current, and therefore give four times the starting torque. This method would not affect the losses at full load appreciably. The over-all effect, however, would usually be to decrease the power factor through increased magnetizing current. This method of increasing starting torque has possibilities, but leads to higher starting currents and bad power factor.

Table I. Effect of Rotor Resistance on Motor Rating

Frequency	Rotor Resistance	Rated Load		Maximum Torque		Starting Torque	
		Horsepower	Torque, Pound-Feet	Pound-Feet	Per Cent of Rated	Pound-Feet	Per Cent of Rated
60	1.0	1.0	3.0	9	300	6.0	200
400	1.0	6.7	2.9	16	510	1.8	60
400	20	2.4	1.1	16	1,500	16	1,500
400	2.8	5.2	2.3	16	680	4.8	200

A third way of increasing the starting torque is to alter the motor so that the resistance or reactance, or both, will vary as the motor comes up to speed. In this classification are included double-cage rotors, external rotor resistance operated through centrifugal switches, and many ingenious devices. The small physical size of aircraft motors makes this method quite difficult, but some desirable solutions will be obtained by this method. In fact, all 400-cycle motors take advantage of some variation in rotor resistance from standstill to full load because of the current distribution due to leakage flux in the rotor bars.

A fourth possibility of increasing the starting torque of the motor is by the use of some mechanical device such as a centrifugally operated clutch which would permit the motor to attain speed before picking up the load.

Economy in weight and efficiency of electrical motors can be obtained by designing the correct load so that large starting torques are not required.

Next in importance to the problem of starting torque is the problem of what to do with the maximum torque developed by the induction motor. As noted in the list of requirements, the airplane designer uses such low factors of safety that the maximum torque applied to the load must be definitely limited, sometimes to as little as 20 per cent in excess of the required torque. This same problem gave some difficulty in d-c motors because of voltage and temperature variations. The series motor, however, develops maximum torque at standstill, which is very desirable for most aircraft applications. We have seen that altering the induction motor so that the maximum torque is obtained at standstill results in lower efficiency and consequent increase in pounds per horsepower. In any case, the specified range of voltage and frequency will cause variations in the maximum torque, probably in excess of 20 per cent.

Therefore, it is necessary either to build the structures sufficiently heavy to withstand considerable maximum torque or to use some method of limiting the torque other than motor design. In d-c motors a slip clutch is used successfully since the speed drops off rapidly with increased torque. With an efficient 400-cycle motor the speed at maximum torque is very high, and if a slip clutch is used the motor will heat very rapidly. The thermal protecting device must then operate to open the circuit. Probably a better solution would be the use of a switch operated directly by torque which would open the motor circuit. The use of shear pins in some form is another possible solution.

The problem of the wide range of ambient temperatures does not affect the 400-cycle motor in the same way it affects the 24-volt d-c motor. Low temperature decreases the resistance, which in the case of the d-c motor increases the torque. In

an induction motor, the low temperature decreases the starting torque. This difficulty of decreased torque is of academic interest only, however, since the rotor currents will rapidly bring the motor up to operating temperature.

The humidity requirements are easier to meet with the induction motor. In developing motors for use in such equipment as submersible pumps for the Navy, methods of impregnating stators have been brought to a much higher degree of perfection than ever before. We do not anticipate any difficulty due to humidity as it affects insulation.

Altitude will have no effect on the electrical operation of induction motors, except as it influences rating by changing the cooling. Considerable data on this effect has been obtained in the use of d-c motors, and these data are used in the design of 400-cycle motors.

One difficulty which requires close cooperation between the airplane electrical engineer and the motor designer is the question of line drop. We have frequently encountered difficulty in operating high-frequency motors in wind-tunnel testing. For instance one motor developed about 35 horsepower at 6,000 rpm, from 300-cycle service. The short leads used between the control panel and the motor installed in the model doubled the resistance and reactance of the stator. In other words, the line drop equalled the stator drop. This difficulty can be overcome by including the line characteristics in predicting the motor performance. With minimum-size lines, starting torque may be seriously affected if starting current is high.

There is a tendency among some writers of specifications to avoid the use of gear reductions in favor of multipole motors. An eight-pole one-horsepower motor will be twice as heavy as a four-pole one-horsepower motor operating from the same frequency. In addition, increasing the number of poles tends to lower both the efficiency and the power factor. This undesirable effect is greatly pronounced in motors of small size. In addition, the starting torque is relatively lower.

We might summarize this discussion by saying that the 400-cycle squirrel-cage induction motor has great possibilities for increasing the horsepower per pound. Difficulties to be overcome before these possibilities can be completely realized have been discussed. These difficulties are not insoluble. The company with which the writer is associated and many other electrical designing groups have some solutions to all of these problems, and many more solutions will be found.

We have pointed out the necessity of the closest co-operation between the designer of motor and power-supply equipment, the airplane designer, and the control designer. With this co-operation and a clear understanding of the problems involved, the 400-cycle aircraft power-supply system will be a marked success.

Field Determination of Current-Transformer Errors by the Secondary-Voltage Method

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FOR many years there has been a need for a relatively simple field method for testing bushing current transformers for accuracy.

A paper by A. C. Schwager[4] describes a method of predetermining a current transformer's performance based on its admittance-vector locus. A study of this paper suggested the possibility of developing a method of testing such current transformers in the field.

Explanation of Secondary-Voltage Method

In the method to be described in this paper, progressively increasing voltages are applied to the secondary winding of a current transformer with the primary winding open-circuited. In this condition the current transformer builds up an opposing reactive voltage nearly equal to that applied. Thus, only a relatively small amount of current is allowed to flow in the secondary winding. This "exciting" current is made up of two components, that is, a "magnetizing" component and a "core-loss" component. The purpose of this method is to analyze the "exciting" current into its components with the object of combining them by formula with the normal secondary currents and thereby find the phase-angle errors and ratio correction factors for the current transformer under test at certain secondary current values, such as, 0.5, 1, 2, 3, 4, and 5 amperes. Before starting the test the current transformer should be demagnetized, with its primary winding open-circuited, by gradually increasing the secondary-winding test voltage from zero to 50 or 100 volts and back to zero, taking care to protect the test instruments during this operation.

To analyze the exciting current into its two components it is passed through the current coil of a wattmeter, the potential coil of which is energized first by a test voltage (E_v) which is 90 degrees out of phase with the induced secondary reactive voltage (E_s) and then by another test voltage (E_w) which leads E_v by 90 degrees, or is itself in phase with E_s. The vars measured in the first case, with voltage E_v, hereafter shall be referred to as V, and the watts measured with voltage E_w, as W.

The magnetizing component of the exciting current, or that portion which is used to produce voltage E_s, is equal to V/E_v, and the core-loss component is W/E_w.

Susceptance (B), or amperes of magnetizing component per induced volt, is V/E_vE_s, and conductance (G), or amperes of core-loss component per induced volt, is W/E_wE_s.

The higher the resistance (R) and reactance (X) of the secondary burden through which the current transformer has to force the secondary current, the higher will be the value of secondary voltage (E_s) that has to be induced. Consequently, the greater will be the core losses in producing it.

It is convenient to express the errors of current transformers in terms of the phase-angle error (ϕ) and the ratio correction factor (RCF). The phase-angle error may be expressed in terms of its tangent and the four quantities B, G, X, and R.

$$\text{Tan } \phi = \frac{BR - GX}{1 + BX + GR}$$

Ratio correction factor (RCF) is expressed in terms of the same quantities and is equal to $(1+BX+GR)/\cos \phi$. For angles less than $2^1/_2$ degrees the RCF may be expressed as $1+BX+GR$ with an error of less than one tenth of one per cent.

Figure 1 shows the connections required for making the aforementioned tests.

Description of Equipment and Test Procedure

The regulating transformers furnish a means for adjusting the test voltage. If an autotransformer is used for this purpose care must be taken to disconnect completely the secondary winding of the current transformer from ground, as ordinarily the supply voltage already will be grounded.

Potential from the regulating trans-

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formers is applied to the current-transformer secondary winding and a compensating burden. For a multi-ratio current transformer a ratio, such as 200–5 amperes, is preferably selected for the tests. The errors for other ratios then may be calculated from curve data obtained at the 200–5-ampere ratio. With switch S in the upper position a voltage of 0.1, 0.2, 0.5, 1, and 2 volts is applied by means of a 20–2-volt service transformer. For voltages greater than 2 it may be applied directly with switch S in the down position. Then the steps applied for the test are 5, 10, 20, 50, and even as high as 100 or more volts.

Table I

E_a	V	E_v	W	E_w	V/E_v	W/E_w	E_s/N	B	G
0.1									
0.2									
0.5									
1									
2									
5									
10									
20									
50									
100									

A compensating burden is introduced into the circuit in order to match the total burden of the current-transformer secondary winding and leads, the wattmeter current coil, and the test leads. The 50–50 volt service transformer is used to subtract this voltage drop, together with the drop across the compensating burden itself, vectorially from the applied voltage (E_a). It thereby secures the true induced secondary voltage (E_s) for use in the phase-angle detector and in indicating voltmeter 2.

Voltmeter 1 is used to determine the values of the voltages (E_v and E_w) that are applied to the wattmeter potential coil when vars (V) and watts (W) are read on the wattmeter.

The phase-angle detector shown in Figure 1 is an electrodynamometer type of instrument equipped with two voltage coils. It has a fixed coil rated 120 volts and a rotor coil which is designed for variable voltages with full-scale ranges of 1, 5, 25, and 125 volts. The inductance of its coils may be neutralized with suitable shunt capacitors.

A phase-angle meter with two voltage elements also is required for the tests. A synchroscope with a 360-degree scale and a light moving element mounted with its shaft in the vertical position can be calibrated in degrees to serve as a phase-angle meter.

During the test the voltage (E_v) is shifted with the small knob on the phase shifter until it lags E_s by 90 degrees, at which time there will be zero deflection on the phase-angle detector. Vars are read as V on the wattmeter, and voltage (E_v) is read and recorded also. Next, the moving element of the phase shifter is rotated counterclockwise until a voltage is obtained that leads E_v by 90 degrees, as indicated by the phase-angle meter. This voltage (E_w) is now known to be in phase with voltage E_s. Maximum deflection of the phase-angle detector occurs at this time. A record is kept of the watts (W) indicated by the wattmeter and of the volts (E_w).

It will be found that, as the voltage is increased from very low values, the readings of both V and W will increase slowly at first and then quite rapidly for the higher-voltage values.

By using a combination wattmeter–varmeter, the 90-degree phase shift is obtained without other adjustments, and the phase-angle meter then may be omitted.

Calculations will be facilitated and repetition of test conditions kept to a minimum if the test quantities are given as in Table I.

It should be noted that $B = V/E_s E_v$ and $G = W/E_s E_w$ where N is the actual turn ratio of the current transformer under test.

Figure 1. Wiring diagram of equipment for testing a current transformer of known turn ratio by the secondary-voltage loading method

The test equipment is as follows:

Regulating transformer, 120 volts, 60 cycles, manually operated

Step-down transformer, 60 cycles, 20 to 2 volts

Insulating tranformer, 60 cycles, 50 to 50 volts with a very low exciting current, for use as an autotransformer

Compensating burden composed of resistance (R_d) and reactance (X_d)

Double-throw selector switch (S)

Voltmeter 1, 0–150 volts alternating current

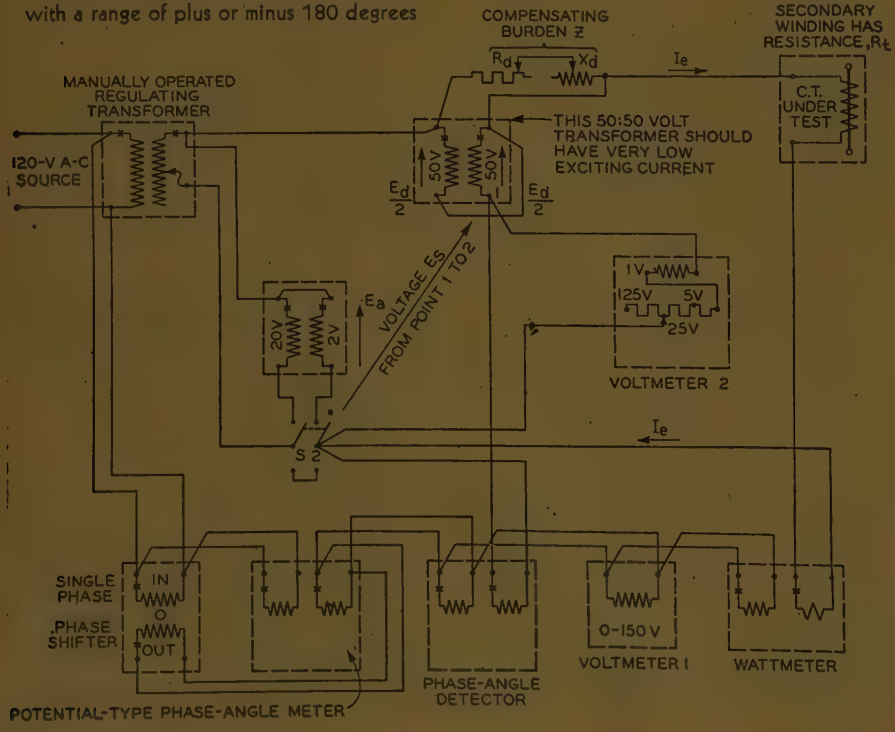
Voltmeter 2, of high resistance with 0–1–5–25–125-volt a-c scales

Phase-angle detector with a 150-volt 60-cycle field coil. It is desirable to have the rotor coil, together with its series resistance, wired for the same operating ranges as voltmeter 2 and to be cut into service by the same selector switch

Wattmeter with scale to suit test conditions, but usually with a 100–200-watt scale

Potential-type phase-angle meter, 120 volts, 60 cycles

Phase shifter, 120 volts, 60 cycles, single phase with a range of plus or minus 180 degrees



Preparation of the Two Characteristic Curves

The values calculated in the last two columns of Table I are plotted vertically against the values of E_s/N on the horizontal scale of log–log cross-section paper. There should be three vertical cycles and at least three horizontal cycles on the log–log paper.

Figure 2 shows typical curves of B and G for a 115-kv bushing current transformer tested at 40 secondary turns and a nominal ratio of 200–5 amperes. From these two curves ratio and phase-angle errors may be calculated for any burden and any ratio within the range of the test data.

The diagonal lines for the various current-transformer ratios are drawn as follows:

1. Draw the diagonal base line for the 200–5-ampere ratio, as shown, between 0.001 on the lower left hand margin and 1.0 on the upper margin.

2. Draw parallel lines starting at distances along the vertical scale equal to the ratio

$$\left(\frac{\text{Actual turn ratio}}{40}\right)^2$$

and along the horizontal scale at distances equal to

$$\left(\frac{40}{\text{actual turn ratio}}\right)^2$$

Application of Characteristic Curves

Since the two characteristic curves are plotted as in Figure 2, the values of B and G and hence the errors may be determined for any current-transformer ratio at any secondary current and burden. For example, at the 100–5-ampere ratio with a secondary burden of $R=0.738$ and $X=0.307$ ohm, with 5 amperes secondary current, the secondary volts (E_s) will be $5\sqrt{0.738^2+0.307^2}$, or 4. For a current-transformer ratio of 100–5 amperes, E_s/N is 4/20 or 0.2. A vertical line is drawn at $E_s/N=0.2$ to intersect the G and B curves as shown. Horizontals from these curves intersect the 100–5-ampere diagonal line at values of 0.091 for G and 0.157 for B. Thus, the phase-angle and ratio errors are found as follows:

$$\text{Tan } \phi=\frac{BR-GX}{1+BX+GR}$$

$$=\frac{(0.157)\ (0.738)-(0.091)\ (0.307)}{1+(0.157)\ (0.307)+(0.091)\ (0.738)}$$

$$=\frac{0.1160-0.0279}{1+0.0482+0.0672}=\frac{0.0881}{1.1154}=0.0791$$

$$\phi=+4^\circ\ 31^1/_2{}'\ \cos\phi=0.9969$$

$$RCF=\frac{1+BX+GR}{\cos\phi}=\frac{1.1154}{0.9969}=1.1185$$

It is usually found convenient to list the quantities as shown in Table II.

Conclusions

From the foregoing discussion it is apparent that a current transformer with primary and secondary turns known may be checked in the field at its secondary terminals in a substation if desired. For bushing current transformers an intermediate ratio, such as 200–5 amperes, is chosen and a range of test voltages applied to the secondary winding varying from 0.1 volt to 100 or more volts. It is not necessary to completely disconnect the current transformer from the high-voltage circuit, provided its primary is open-circuited. In this case one side of the current-transformer secondary winding should be kept grounded.

Test equipment may be kept to a minimum, if approximate results are sufficient, by applying the secondary voltage at the current-transformer terminals. The phase shifter, 50–50-volt transformer, compensating burden, and phase-angle detector then may be omitted. The error in this instance will be caused by applying a voltage, E_a, that differs from the induced secondary voltage, E_s, by the voltage drop in the secondary winding and test leads caused by the passage of the test current. If the secondary test voltage is kept below ten volts in testing a bushing-type current transformer of 200–5 amperes, the error in the results from neglecting this difference should be less than two per cent.

From the field data two curves are plotted on log–log cross-section paper with one curve showing the susceptance (B) of the open-circuited current transformer, and the other showing its conductance (G). Values of E_s/N are used for the horizontal scale in each case. Parallel diagonal lines are drawn on the log–log paper to represent all available ratios, and the sheet is then ready for use in determining the ratio and phase-angle errors of any burden at any power factor for any current-transformer ratio, within the limits of the data taken. The RCF in terms of a nominal turn ratio is equal to $\frac{\text{actual turn ratio}}{\text{nominal turn ratio}}$ multiplied by (actual RCF).

For metering use the limits 0.001 to 1.0 for E_s/N at the 200–5-ampere ratio covers most of the required metering ranges for all standard bushing-current-transformer ratios. However, for relay use it would be better in all probability to make the tests at 100–5 or at 50–5 am-

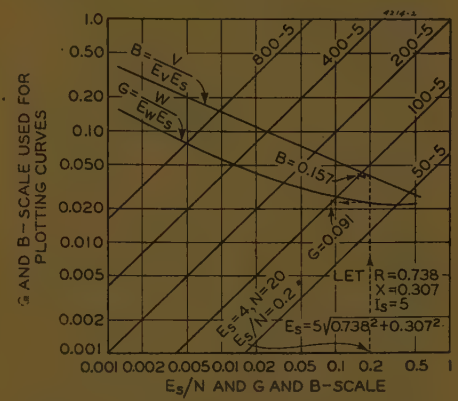


Figure 2. Curves of G and B plotted from test data for a multiratio current transformer at 200–5 amperes

Values of G and B at any secondary burden and any available ratio are determined as indicated by the arrows

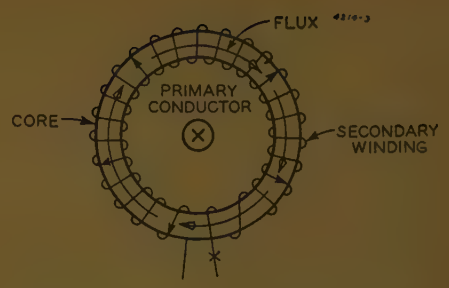


Figure 3. Typical multiratio bushing current transformer

The winding of one ratio only is shown

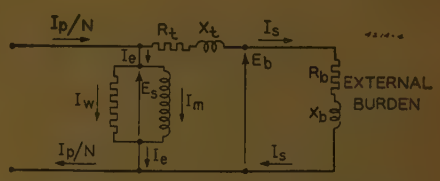


Figure 4. Equivalent circuit of a current transformer in service under load conditions

Summary of the terms used:

R_b = ohms resistance of external secondary burden

X_b = ohms reactance of external secondary burden

R_t = ohms resistance of current-transformer secondary winding. Its effect is the same as if it were connected externally in the circuit as shown

X_t = ohms reactance of current-transformer winding. This is usually so small that it may be neglected

I_w = amperes core loss which = $W/E_w = GE_s$

I_p = amperes primary current

N = turn ratio between secondary and primary turns of current transformer

I_p/N = equivalent primary current (amperes) on a 1-to-1 ratio basis

I_s = amperes flowing in current-transformer secondary windings

I_e = amperes exciting current made up of components I_m and I_w. $I_e=\sqrt{I_w^2+I_m^2}$

I_m = amperes magnetizing current and equals $V/E_v = BE_s$

E_b = volts applied to external burden

Table II

I_s	E_s/N	B	G	BX	BR	GX	GR	BR − GX	BX + GR	Tan ϕ	ϕ	Cos ϕ	RCF
0.5	—	—	—	—	—	—	—	—	—	—	—	—	—
1	—	—	—	—	—	—	—	—	—	—	—	—	—
2	—	—	—	—	—	—	—	—	—	—	—	—	—
3	—	—	—	—	—	—	—	—	—	—	—	—	—
4	—	—	—	—	—	—	—	—	—	—	—	—	—
5	—	—	—	—	—	—	—	—	—	—	—	—	—

peres in order to cover the range for which the data may be desired because of the greater saturation at heavy fault currents.

It is likely that modifications of the curves will yield other data useful in relay and other engineering calculations. However, the purpose of the curves and data as here presented is to describe a field method that may be used primarily to determine the phase-angle errors and ratio correction factors of installed current transformers, of known turn ratio, for metering purposes. Generally this will apply to uncompensated bushing-type and wound-type current transformers, as compensation usually destroys the true turn ratio.

Appendix

Figure 3 shows a schematic drawing of a typical bushing-type current transformer. The multiratio taps are not shown. However, a well-designed multiratio current transformer will be constructed so that, for any ratio, the turns will describe at least one complete helix around the core. For this reason there is very little inducement for the flux to go any place except around the core within the helix. Since the leakage flux is extremely small the reactance component of the short-circuited impedance of the current transformer should be negligible. The actual d-c resistance of the portion of the secondary winding being considered always should be added to the burden resistance to obtain the resistance (R) used in the expressions for tan ϕ and RCF and in evaluating E_s.

In Figure 4 an equivalent circuit is shown for a current transformer. On a 1:1 turn-ratio basis, the total primary current (I_p/N) is made up of two parts, I_e, the exciting current, and I_s, the secondary current. The exciting current is dependent upon the amount of secondary voltage per turn that is required. Low secondary voltage per turn means low values of exciting current. High secondary voltage per turn means high exciting current.

The exciting current is made up of two components, I_w, the core-loss component which is in phase with E_s, and I_m, the magnetizing component, which lags E_s by 90 degrees.

The voltage induced per turn in the secondary winding and the total core losses are constant for any constant value of magnetic flux flowing around the core.

Conditions of voltage or coil turns which produce the same core flux will have definite and determinable relations existing between them.

Since the value of flux in the core is proportional to E_s/N (from the formula based on Lenz's Law, $E=4.44NF\phi$ max (10^{-8})) any change in the value of E_s attended by a proportionate change in N for a given current transformer will result in the same value of core flux and in the same total core losses. The core flux is directly proportional to the secondary-induced volts per turn, E_s/N and, for any given value of E_s/N, is also proportional to the magnetizing ampere turns, NI_m or NE_sB. Consequently, E_s/N is also proportional to E_sNB, or B is proportional to $1/N^2$.

The core losses are equal to E_sI_w or $E_s(E_sG)$. They are not necessarily proportional to E_s/N. However, for any given value of E_s/N they are constant and equal to E_s^2G. Hence, if N is made to vary, the corresponding value of E_s for the same core loss as before must vary in direct ratio to N. Therefore, under this condition it may be said that the core loss E_s^2G, is equal to K, a constant, or that G equals K/E_s^2, where E_s is proportional to N.

Thus, G equals K_1/N^2 for any given values of ϕ, E_s/N, and core loss. K_1 is a proportionality constant.

Furthermore, if the value, G_1 and B_1 are known for a current transformer of turn ratio N_1 at a given value of E_{s1}/N_1, they will be equal to these values multiplied by the ratio $(N_1/N_2)^2$ for any other ratio, such as $^1/_2$, provided E_{s2}/N_2 is equal to E_{s1}/N_1. These are the relations upon which the calculations of Figure 2 are based.

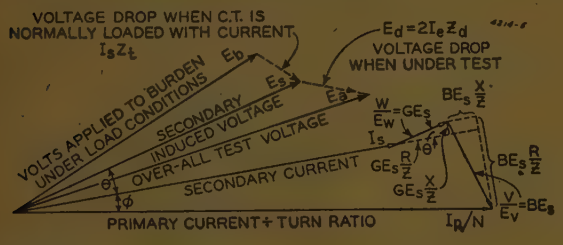


Figure 5. Vectorial representation of voltage and current values within a current transformer carrying a very heavy secondary burden

The diagram shows current and voltage vectors under load and under test conditions

Explanation of the terms used:

R, X, and Z = ohms resistance, reactance, and impedance, respectively, of the current transformer together with its total external secondary burden

$Z=\sqrt{R^2+X^2}$

ϕ = phase-angle error of current transformer in degrees

θ = characteristic phase angle of secondary burden

Tan $\theta=X/R$

E_a = volts applied to secondary circuit under test

Z_d = ohms impedance of secondary compensating burden

E_d = volts drop under test conditions across the total secondary circuit the impedance of which is equal to $2Z_d$

$E_d=2I_eZ_d$

For explanation of other items used in Figure 5 see caption for Figure 4

If it is found that the range of operation is too limited with the three-cycle log-log paper the horizontal scale may be extended to four or five cycles.

Figure 5 shows the vector relations with the exciting and core-loss components of current considerably exaggerated. It may be noted that the primary current I_p/N is vectorially equal to the sum of $I_s+GE_s+BE_s$ and algebraically equal to:

$$\frac{I_s+GE_s\left(\frac{R}{Z}\right)+BE_s\left(\frac{X}{Z}\right)}{\cos\phi}$$

where

$$Z=\sqrt{R^2+X^2}$$

The phase-angle error may be found from the tangent of the angle ϕ between I_p/N and I_s, this angle being considered positive when I_s leads I_p/N,

$$\text{Tan }\phi=\frac{BE_s(R/Z)-GE_s(X/Z)}{I_s+GE_s(R/Z)+BE_s(X/Z)}$$

$$=\frac{BRI_s-GXI_s}{(1+BX+GR)I_s}$$

$$=\frac{BR-GX}{1+BX+GR}$$

The ratio correction factor is equal to the ratio I_p/N divided by I_s. Also, $E_s/I_s=Z$. Likewise, from Figure 5 it is apparent that

$$RCF=\frac{I_s+BE_s\frac{X}{Z}+GE_s\frac{R}{Z}}{\cos\phi}$$

or:

$$\frac{I_p/N}{I_s}=\frac{\frac{I_s}{I_s}+\frac{BE_s}{I_s}\frac{X}{Z}+G\frac{E_s}{I_s}\frac{R}{Z}}{\cos\phi}$$

or the

$$RCF=\frac{1+BX+GR}{\cos\phi}$$

References

1. A Study of Current Transformers With Particular Reference to Core Losses, P. G. Agnew. *Bulletin* of Bureau of Standards, volume 7, 1911, pages 423–74.

2. Overcurrent Performance of Bushing-Type Current Transformers, C. A. Woods, Jr., S. A. Bottonari. AIEE Transactions, volume 59, 1940, September section, pages 554–60.

3. Analysis of Bushing-Current-Transformer Performance, J. P. Gebelein, J. A. Elzi. AIEE Transactions, volume 59, 1940, September section, pages 541–6.

4. Current-Transformer Performance Based on Admittance-Vector Locus, A. C. Schwager. AIEE Transactions, volume 61, 1942, January section, pages 26–30.

5. Current-Transformer Output and Application Charts, R. Koller, AIEE Transactions, volume 63, 1944, August section, pages 573–7.

D-C Arc Interruption for Aircraft

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MEMBER AIEE

THE present aircraft electric system uses 24 volts direct current; the future system will assuredly be of a higher voltage as aircraft and their generating systems increase in size. Whether it be direct current or alternating current depends on the relative merits of each.[1] If the new system be alternating current the problem of arc interruption is not difficult. If the new system be direct current and above 24 volts, it has been assumed that the arc-interruption problem at altitude would be quite difficult. There is, however, little quantitative data available to bear this out. An investigation was therefore initiated to obtain preliminary data on arc-interruption problems introduced by the use of higher d-c voltages.

The use of a newly developed timer for measuring the duration of arcing made it possible to obtain accurately and rapidly a large amount of quantitative data which are presented herewith.

Scope of Paper

This paper presents data on d-c arc interruption for voltages up to 250 volts at pressures from sea level to 50,000 feet altitude. Both resistive and inductive loads are investigated. The load range chosen includes small currents corresponding to relay or auxiliary-contact values, and higher currents associated with power circuits. Two general types of devices were used for the tests; namely, single- and double-break switches and a blowout-type contactor. The results are presented both in the form of maximum amperes which can be interrupted and arcing time in milliseconds. The effect of inductive loads on arc interruption is discussed briefly.

There also are presented the results of an investigation on the effect of a magnetic field on an arc, which at altitude tends to produce an arc movement in a direction opposite to that at sea level.

No attempt has been made to give theoretical or analytical explanations of the results obtained.

A parallel investigation of the voltage peaks resulting from current interruption was made at the same time and the results of this investigation are presented in a paper by Phillips and Mitchel.[2]

Conclusions

1. The interrupting ability of small gaps (of the order of 0.020 inch) increases markedly with altitude for voltages above a certain minimum (about 80 volts).

2. The interrupting ability of larger gaps (of the order of 0.220 inch) increases with altitude for voltages above a certain minimum (about 175 volts). For these larger gaps the increase is not as marked as for the smaller gap.

3. On a 30-volt system the arcing time *increases* with altitude. On a higher-voltage system (above some minimum depending on the gap) arcing time *decreases* with altitude.

4. For double-break devices handling resistance loads, the maximum volt-amperes which can be interrupted increases rapidly as voltages decrease below 100 volts.

5. The interrupting ability of a double break, each gap being 0.020 inch, on a 30-volt system is about five times that of a single 0.040-inch gap.

6. The arc-interrupting ability of a device improves with increased humidity both at sea level and at reduced pressure corresponding to 60,000 feet.

7. A blowout-type contactor of the type used in industrial work will handle the power requirements of aircraft (100 kva and above at voltages up to 250 volts direct current both at sea level and altitude. Special attention must be paid in the design to ensure successful operation with low currents at altitude.

8. A reverse blowout effect exists at altitude which tends to move a low-current arc under the influence of a magnetic field in a direction opposite to that at sea level. This effect was not observed at currents above 50 amperes. The reverse forces apparently are quite small.

9. There is an effect observable with small gaps on direct current, similar to that on alternating current, whereby it is possible to increase the maximum interrupting capacity of a pair of contacts by *decreasing* the gap.

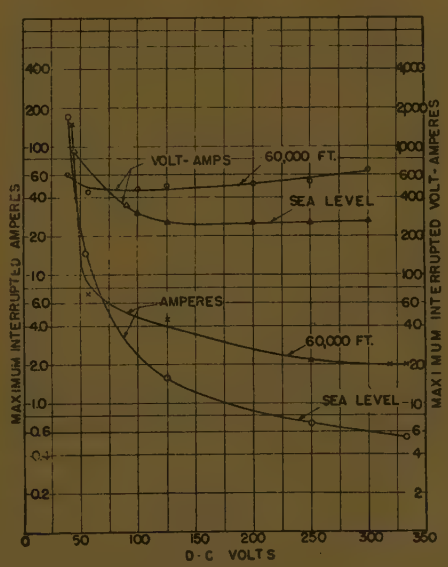


Figure 1. Comparison of the interrupting ability versus voltage at sea level and 60,000 feet altitude for the double-break Switchette on resistive load

Gap: 0.020 inch per break

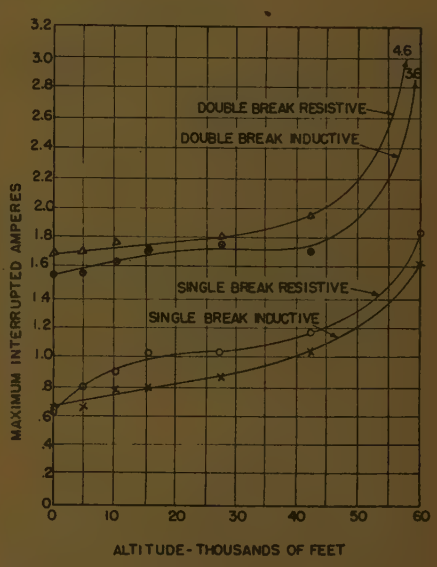


Figure 2. Maximum interrupted current versus altitude for the double-break Switchette at 125 volts direct current on resistive and inductive loads, both single and double break

Load: Inductive *B*, Table I
Gap: 0.020 inch per break

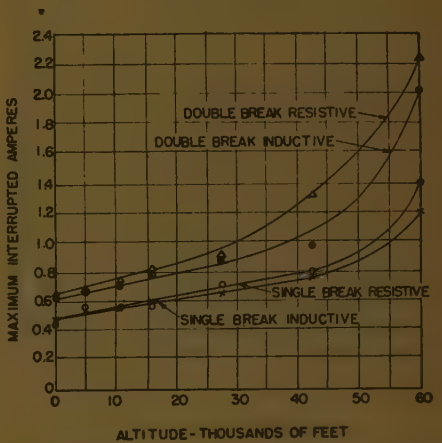


Figure 3. Maximum interrupted current versus altitude for the double-break Switchette at 250 volts direct current on resistive and inductive loads, both single and double break

Load: Inductive *B*, Table I
Gap: 0.020 inch per break

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The authors acknowledge the assistance of J. P. Kone who was responsible for a large part of the data on the Switchette.

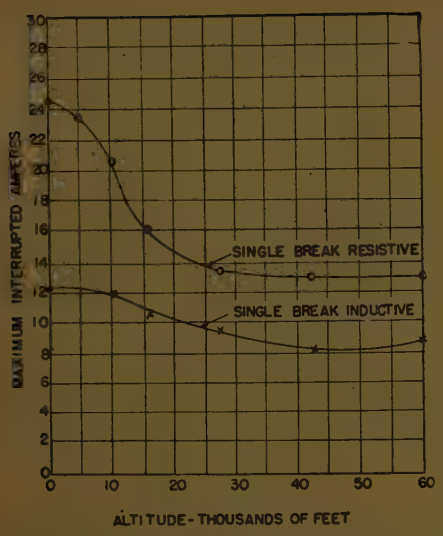


Figure 4. Maximum interrupted current versus altitude for the Switchette at 30 volts direct current on resistive and inductive loads, single break

Load: Inductive *B*, Table I
Gap: 0.020 inch per break

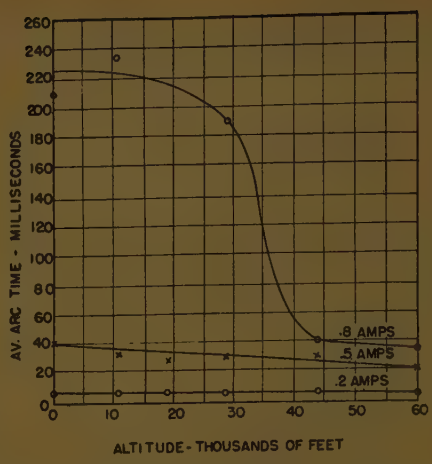


Figure 6a. Average arc time versus altitude for the double-break Switchette at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of these for higher currents
Gap: 0.020 inch per break

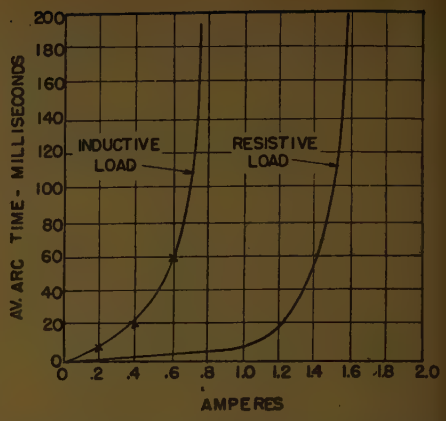


Figure 6b. Average arc time (ten readings) versus current for the double-break Switchette at 125 volts on both resistive and inductive loads at sea level

Load: Inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of these for higher currents
Gap: 0.020 inch per break

10. The maximum interrupting capacity of a device is about the same for loads which have low inductance (inductive *B*, Table I) as for resistance loads. Highly inductive loads (inductive *A*, Table I) lower the interrupting capacity by as much as two to one.

Devices Used

All data presented in this paper were taken on the following three devices:

1. An aircraft switch known as the Switchette. This is a small double-break switch rated at ten amperes, 30 volts, single circuit, inductive load, at all altitudes. It has silver tips of one-eighth-inch diameter and a gap of 0.020 inch. It requires from one to two milliseconds for the tips to travel across the 0.020-inch gap.

2. A double-break switch with a bridge-type movable contact arm carrying silver tips about one-half inch in diameter on seven-eighth-inch centers. The tip gap was set at 0.220 and 0.312 inch. This unit was part of a commercial size 1 contactor having a tip rating on alternating current of 25 amperes and a motor rating of $7\frac{1}{2}$ horsepower at 550 volts. The opening speed is about 20 inches per second.

3. An industrial blowout-type contactor with copper tips rated at 100 amperes, 600 volts, direct current. The gap length on this device is $\frac{19}{32}$ inch and there are 11 turns in the blowout field. In this device the arc transfers from the tips to stationary arcing horns so that the opening speed does not usually affect arc interruption.

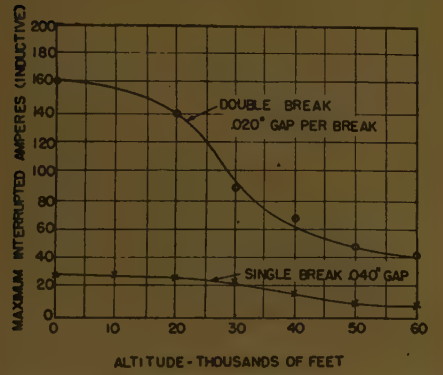


Figure 5. Comparison of maximum interrupted current versus altitude for the 0.040-inch-gap single-break Switchette and the 0.020-inch-per-break double-break Switchette on inductive load at 30 volts direct current

Load: Inductive *B*, Table I

Method of Test

Power for most tests was supplied by one or two generators rated 200 kw at 300 volts. For some of the tests at 30 volts on the Switchette a battery table capable of supplying 2,000 amperes was used. Arcing times were obtained with the use of a specially built electronic arc timer. The input impedance to this device is about four megohms so that its effect on the arcing characteristic of the devices was negligible.

The device under test was mounted in a transparent bell jar which was evacuated to simulate altitude. Pressure was read on a vacuum gauge calibrated against a manometer. Conversion between pressure in inches of mercury and altitude was based on data from National Advisory Committee for Aeronautics report 218. No attempt was made to control temperature or humidity except in one instance when the humidity was held at 100 per cent for a test at both sea level and altitude. The device was operated at 20 make and breaks per minute for the lower currents (100 amperes and below) and about five to ten operations per minute at the higher currents. In all cases sufficient on-time was allowed for the current to reach its steady state value before breaking the circuit. Ten readings were taken at each set of conditions when obtaining arc-time data so that each point on the presented curves is the average of ten readings. To obtain the data showing the maximum current which a device could handle, the current was increased in small steps taking ten interruptions at each point, until the current was found at which the arc would not extinguish.

Loads—Inductive

The loads used in these tests are those designated in Table I. The inductance

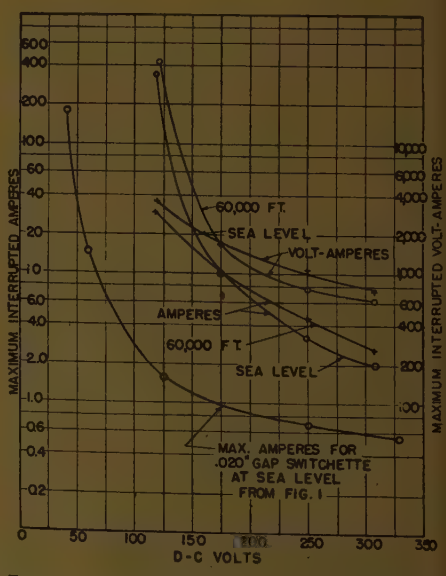


Figure 7. Comparison of the interrupting ability versus voltage at sea level and at 60,000 feet for the 0.220-inch-gap-per-break double-break contactor on resistive load

One curve for the Switchette from Figure 1 is shown for comparative purposes

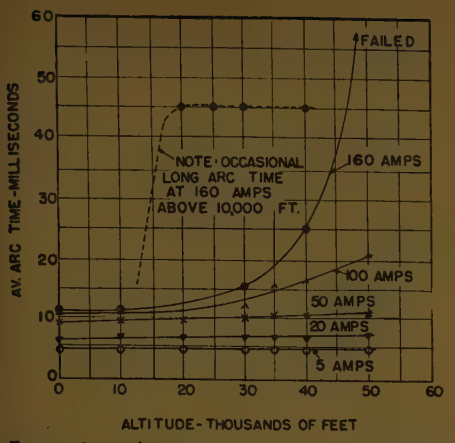


Figure 8a. Average arc time versus altitude for the double-break contactor at 60 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive C, Table I
Gap: 0.220 inch per break

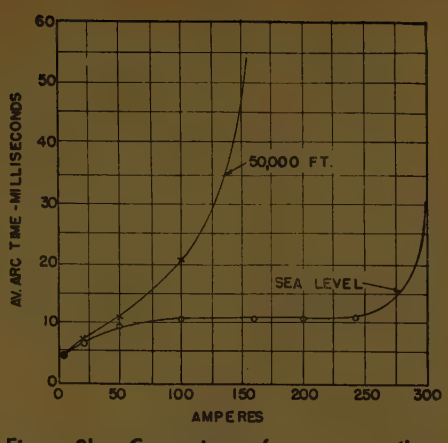


Figure 8b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet for the double-break contactor on inductive load at 60 volts direct current

Load: Inductive C, Table I
Gap: 0.220 inch per break

given is that defined as differential inductance.[3] This inductance is obtained from a step-by-step solution of the oscillogram of current rise under constant voltage using the equation $Ldi/dt+Ri=E$. Although the values of inductance are given only at 50 per cent and 100 per cent current, a continuous curve of inductance versus current can be obtained from the oscillogram.

Discussion of Data

Table II shows the average arc time on a 30-volt system for several commercially available aircraft devices. It is presented here merely to give some factual data and establish the order of magnitude of the arcing times which are now obtained.

Switchette, 0.020-Inch Gap

Figures 1 through 6 inclusive present results obtained with the Switchette. Figure 1 is a plot of maximum interrupted amperes and maximum volt-amperes versus voltages at sea level and 60,000 feet for resistance load. The rather surprising result should be noted that for voltages above 80 volts interruption *improves* with altitude. The complete curves of maximum amperes versus altitude for 125 volts direct current and 250 volts direct current which were taken for an inductive and a resistive load, both single and double break, are given in Figures 2 and 3. Again it will be noted that interrupting capacity increases with altitude.

Figure 4 compares interruption for resistive and inductive loads versus altitude at 30 volts direct current. Figure 5 shows a comparison of the interrupting ability on inductive load of a double break of 0.020 inch per break with one single break of 0.040-inch gap. The large interrupting ability of the double break at 30 volts is probably due to the fact that the minimum voltage required to maintain an arc in air is approximately 15 volts per break.

Figure 6a shows arcing time versus altitude for three currents at 125 volts. The shorter arcing times at altitude lead to the conclusion that interruption improves with altitude at this voltage, which is in agreement with data shown in Figure 1. Figure 6b shows average arc time versus amperes at 125 volts at sea level for a resistance load and a very highly inductive load. The very sudden change in arc time as the switch approaches its limit is clearly shown. The difference in maximum interrupting ability between this curve and that of Figure 2 is due to the difference in load inductance.

Double Break, 0.220-Inch Gap and 0.312-Inch Gap

Data were taken on a conventional double-break switch for gaps of 0.220 inch and 0.312 inch. Only the data taken at 125 volts are submitted for both gaps; the majority of the results shown are for the 0.220-inch gap.

The maximum current and maximum volt-amperes interrupted versus voltage for 0.220-inch-gap double-break switch are shown on Figure 7 for a resistance load at sea level and 60,000 feet. Again an improved interruption at the higher voltages is noted, but the voltage above which arc interruption improves is approximately 175 volts for this gap. There is only a slight increase in the current which can be interrupted at altitude compared to sea level for voltages above 175 volts; for voltages below this value the interrupting ability is noticeably less at altitude. A comparison of the maximum currents that can be handled at sea level by the 0.220-inch-gap double-break contactor and the 0.020-inch-gap Switchette is also shown in Figure 7.

The double-break switch, when operated on 30 volts direct current, interrupted currents of 1,000 amperes, both resistive and inductive, without difficulty at both sea level and 50,000 feet. No curve has therefore been plotted for this voltage.

Figures 8a and b show the operation of this device at 60 volts with varying altitude. The point of failure occurred between 100 and 160 amperes at 50,000 feet. It is interesting to note that the arc time

Table I. Load Inductance

Load Designation	Load	Load Data	Load Rating 100 Per Cent V	Load Rating 100 Per Cent I	Inductance, Henries a 50 Per Cent I	Inductance, Henries a 100 Per Cent I
Inductive *A*	Series and series-parallel combinations of d-c industrial relays; data given for each relay	9,600 turns, 1.5-square-inch iron area, armature sealed	125	0.42	55	8
Inductive *B*	Parallel combinations of d-c industrial relays; data given for each relay	715 turns, 0.6-square-inch iron area, armature sealed	8	2.6	0.10	0.08
Inductive *C*	D-c series-motor field *MDO*-109, 150 horsepower	Series field and commutating field	220	480	0.002	0.0044
Inductive *D*	D-c series-motor field *MDO*-105	Series field	220	120	0.010	0.018
Inductive *E*	D-c series-motor fields *MDO*-105 and 106 in series	Series fields	220		0.012	0.024
Inductive *F*	Aircraft series motor, 1/13 horsepower	Armature and field in series—rotor blocked	24	17.3	0.0033	0.002
Inductive *G*	Aircraft series motor, one-eighth horsepower	Armature and field in series—rotor blocked	24	31.7	0.003	0.003
Inductive *H*	100-ampere-dynamotor contactor coil	2,300 turns, 0.5-square-inch iron area, armature sealed	28	0.54	2.4	1.5
Inductive *J*	Solenoid-type 200-ampere contactor coil		24	0.42	4.2	1.0
Inductive *K*	Balanced-armature-type 100-ampere contactor coil	3,100 turns, 0.09-square-inch iron area, armature sealed	24	0.19		
Inductive *L*	Four-horsepower aircraft-motor field	Shunt field	24	9.6	0.06	0.03

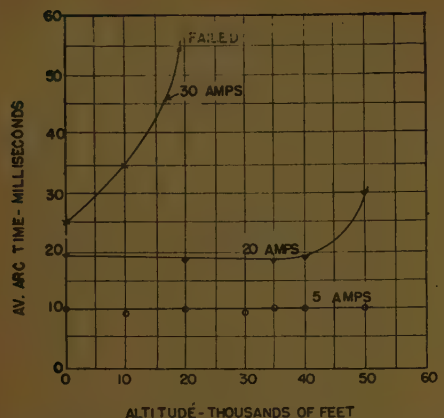


Figure 9a. Average arc time versus altitude for the double-break contactor at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive C, Table I
Gap: 0.220 inch per break

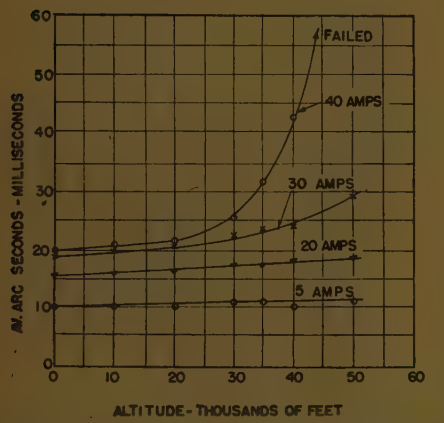


Figure 9b. Average arc time (ten readings) versus altitude for the double-break contactor at 125 volts direct current on inductive load

Load: Inductive C, Table I
Gap: 0.312 inch per break

gave an indication of impending failure at 160 amperes by an occasional reading of long arc time before the altitude was reached at which failure finally occurred.

Figure 9a shows the performance of the 0.220-inch-gap switch while Figure 9b shows the performance of the 0.312 inch gap, both on an inductive load at 125 volts. The increased gap has the larger interrupting ability both at sea level and altitude.

Figures 10a and b are taken at the same voltage as Figure 9, the only difference being a higher inductance load. The point of failure has dropped somewhat lower, and the arcing times are longer at the higher amperes.

On 250 volts direct current this device will interrupt only three or four amperes inductive load. It should be noted from curves 7, 8, and 9 that interruption at altitude does not improve for this gap.

Blowout-Type Device

A magnetic-blowout-type device was used to interrupt the high currents corresponding to the power available on modern aircraft. The first tests consisted of interrupting a 40-kva inductive load (inductive *C*) and measuring the arcing time for voltages from 30 to 250 volts direct current at altitudes up to 50,000 feet. The contactor had no difficulty in handling this constant kilovolt-ampere load and the arc time increased uniformly with voltage from 8 to 15 milliseconds. Three was no appreciable change in arc time with altitude for this load.

Figures 11a, 12a, and 13a show the operation of the blowout-type contactor at 60 volts, 125 volts, and 250 volts in arcing time versus altitude. Figures 11b, 12b, and 13b show arcing time at sea level and 50,000 feet versus amperes. It will be noted that a series blowout-type contactor at altitude has an arc-time characteristic very similar to its characteristic at sea level. This means that the device can successfully handle currents from its rating to ten or more times its rating with essentially constant arc time. A blowout-type contactor inherently has a weak point characterized by long arc times at currents around 20 per cent of its rating. This is the range in which the blowout field, because of the low current, is too weak to be effective and the contactor depends primarily on its large gap to break the current. It will be noted that this contactor, although operating successfully at sea level, failed on 250 volts in the low-current range, indicating that altitude has made the interruption more difficult for those currents where the blowout field is ineffective.

Table II. Comparison of Arcing Times at 30 Volts Direct Current

Load: Inductive C, Table I

Device	Gap Break, Inch	Current, Amperes	Average Arc Time (Ten Readings) Milliseconds at Sea Level	Average Arc Time (Ten Readings) Milliseconds at 50,000 Feet
Switchette	0.020	5	5	5
		10	5	5
		50	12	23
Double-break contactor (referred to in paper—devices used)	0.312	5	4	4
		10	4	4
		20	5	5
		50	7.0	7.5
		112	9.5	12.5
		220	7.0	17
Double-break aircraft contactor rated 50 amperes	0.0625	5	5.5	6
		10	6	6
		20	6.5	7
		30	7	7.5
		50	9	10
		100	19.5	24
		150	25	33.5
Double-break aircraft contactor rated 100 amperes	0.125	150	16	22
Double-break aircraft contactor rated 200 amperes	0.125	150	12.5	18.5

Effect of Humidity

One test was made to find the effect of humidity on arc interruption. For this test a container of water was boiled within the bell jar until moisture condensed on the jar. Arcing times were measured on an inductive load at sea level and 60,000 feet. Similar data were taken on the same device with air which was at 20 per cent relative humidity at sea level. The results of these tests are shown in Figure 14. It will be noted that there is some improvement in arc interruption due to increased humidity.

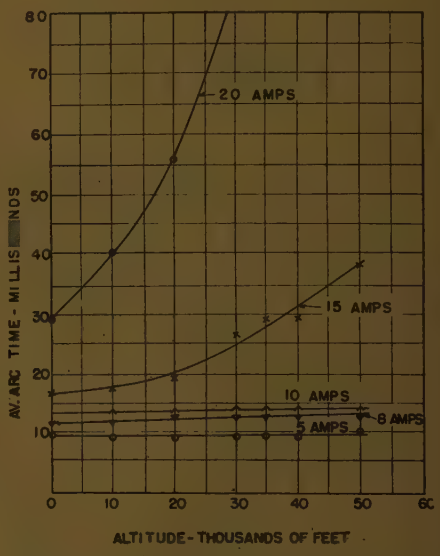


Figure 10a. Average arc time versus altitude for the double-break contactor at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive E, Table I
Gap: 0.220 inch per break

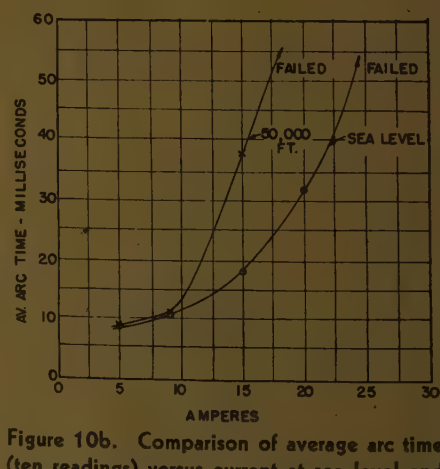


Figure 10b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet for the double-break contactor on inductive load at 125 volts direct current

Load: Inductive C, Table I
Gap: 0.220 inch per break

Reverse Blowout at Altitude

It has been known for some time that at altitude an arc may move under the influence of a magnetic field, in a direction opposite to that in which it moves at sea level. To investigate this effect two devices were used. The first had as electrodes a pair of slightly bowed copper bars mounted to make contact at their center. They were arranged so that the arc could move horizontally, equally well in either direction. A separately excited source supplied blowout-field excitation.

Using only 125-volt d-c supply, arc currents from three to seven amperes, both inductive and resistance loads, and gaps of one-eighth inch, one-fourth inch, three-eighths inch, and one-half inch, it was found that as pressure was reduced the arc moved slower and slower in the normal direction until finally it did not move. The altitude at which this occurred is shown in the following table:

Gap, Inch	Current, Amperes	Direct Voltage	Altitude, Feet
1/8	5	125	38,000
1/4	5	125	46,000
3/8	5	125	47,000
1/2	5	125	56,000

For altitudes above those shown the arc was driven in the reverse direction.

It will be noted that as the gap increased the altitude required to stop all movement in the normal direction also increased. Changing the field strength over a wide range did not appear to affect this phenomenon.

The second device used consisted of a double-break switch with two permanent-magnet blowouts and electrodes so arranged that each arc could travel in either of two directions. Using this device with a gap of 1/16 inch and 3/32 inch it was observed that the reverse blowout effect did not take place on both gaps, one reversing at altitude and the other merely decreasing in velocity. Investigation revealed that there was a dependence on the direction of current flow. If the cathode was in a strong magnetic field near the magnet the reverse blowout occurred but when the cathode was in a weaker field reversal did not occur.

As the pressure in the vacuum chamber is lowered the arc is observed to move slower and slower. It finally stops moving and then if conditions are right moves in the opposite direction. At this point it is a diffuse discharge, its cross section having increased many times. It is easily observable that the different parts of the arc are acted on by opposing forces, for the arc column will sometimes travel in one direction while the cathode spot moves in another. It appears that the reverse blowout forces act primarily on the region near the cathode spots while the magnetic field as usual acts on the arc column. At very small gaps these same phenomena can sometimes be observed at sea level.

In general, the reverse force appears to be quite small and it may be overcome simply by convection. The reverse arc movement has been reported and discussed in a paper by C. G. Smith.[4]

General Discussion

Inductive Loads

There is no standard method of defining the type of load to be used for interruption tests. Often a load is designated merely as "inductive," the inference being that such an inductive load is typical or similar to those which the device must handle. In some cases a time constant for the circuit is specified as in the Navy Aeronautical Specification for contactors *M*569. This is an inadequate designation for it is possible to make up any number of d-c circuits which have the same time constant but whose severity of interruption can be many times different. If an additional specification is made regarding the current at which a certain degree of saturation exists, then a time-constant definition of a circuit is more nearly adequate. Another method of specifying load is to give the dimensions and turns of the test reactor or device. Here again attention must be paid to saturation. The standard test inductors specified by the Bureau of Aeronautics, Navy Department, *E*180, saturate at about 20 per cent of their rated current. This reactor will then have appreciable inductance up to 20 per cent of its rating and negligible inductance thereafter. A more fundamental method of defining the inductance of a circuit for interruption comparisons would be to give the total stored magnetic energy in the circuit. However, this is hard to measure, whereas a curve of inductance versus current can be obtained simply for any combination of devices from the current–time curve.

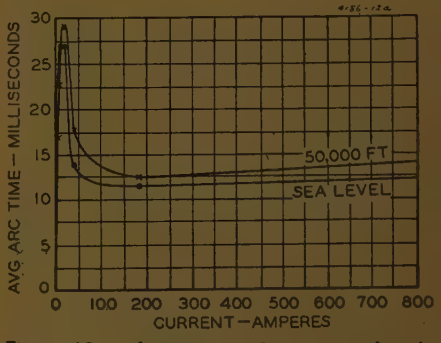


Figure 12a. Average arc time versus altitude for the 0.590-inch-gap blowout-type contactor at 125 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive C, Table I

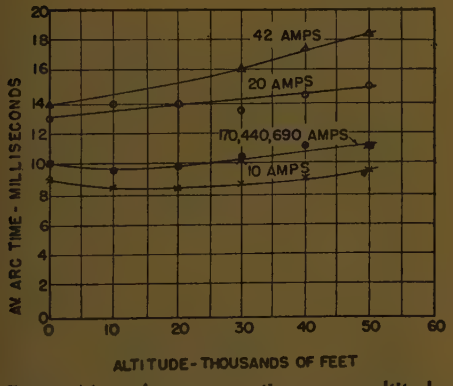


Figure 11a. Average arc time versus altitude for the 0.590-inch-gap blowout-type contactor at 60 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive C, Table I

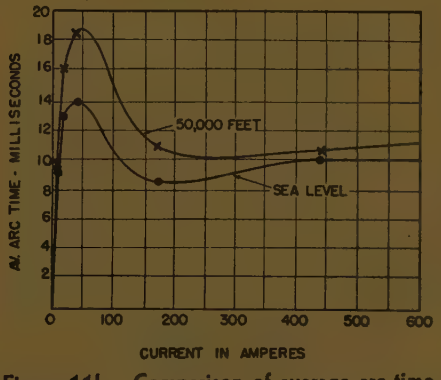


Figure 11b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet at 60 volts direct current for the 0.590-inch-gap blowout-type contactor on inductive load

Load: Inductive C, Table I

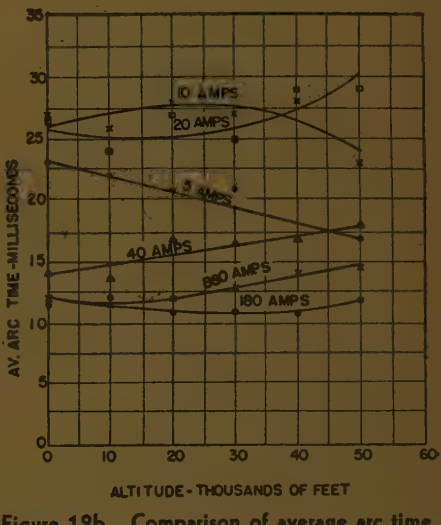


Figure 12b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet at 125 volts direct current for the 0.590-inch-gap blowout-type contactor on inductive load

Load: Inductive C, Table I

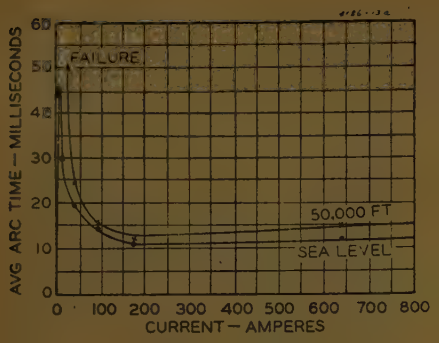


Figure 13a. Average arc time versus altitude for the 0.590-inch-gap blowout-type contactor at 250 volts direct current on inductive load

Each point represents the average of ten readings of arc time
Load: Inductive C, Table I

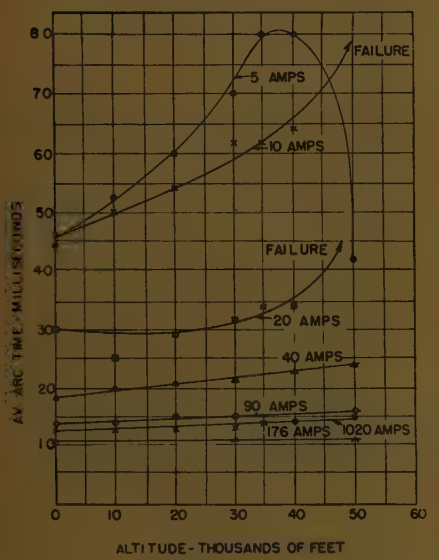


Figure 13b. Comparison of average arc time (ten readings) versus current at sea level and 50,000 feet at 250 volts direct current for the 0.590-inch-gap blowout-type contactor on inductive load

Load: Inductive C, Table I

In this paper differential inductance, which is defined as

$$L = \frac{E - Ri}{\frac{di}{dt}}$$

has been used although it is recognized that it is not the characteristic inductance exhibited by a circuit under arcing conditions. If hysteresis and eddy currents were absent this inductance would be the true inductance to specify for circuit interruption. In any iron-cored circuit, however, especially on direct current where solid iron is used, these core-loss effects are present and so the true inductance which affects arc interruption will vary considerably depending on the current and the speed with which the current is interrupted. The differential inductance is used in this paper because it is readily obtained and is an indication of the inductance of a device.

Arc Time

It should be noted that the curves present interruption data in two forms—actual arc time in milliseconds and maximum interrupting ability. The latter is often the criterion used to establish the interruption rating of a device, the actual rating being a certain percentage of the maximum current which can be broken. This method of evaluating interrupting ability presupposes a short arc time at rated current and for sea-level tests this is usually correct. However, when investigations of interruptions at various pressures are made, it is advantageous to discover by means of actual arc times the point at which erratic performance begins.

Furthermore, arcing time is a good quantitative measure of the comparative performance of devices. In most cases a sudden increase in arcing time anticipates actual failure of the device. Arc time is also important for evaluating tip life.

For small direct currents where high-voltage surges are usually initiated by the arc interruption, the use of a magnetic oscillograph of the usual sensitivity may lead to erroneous values of arcing time and arcing characteristics.

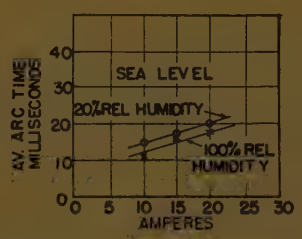


Figure 14a. Comparison of average arc time versus current at 20 and 100 per cent relative humidity with 0.590-inch-gap blowout-type contactor on inductive load at sea level

Each point represents the average of ten readings of arc time
Load: Inductive C, Table I

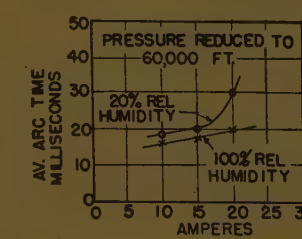


Figure 14b. Comparison of average arc time (ten readings) versus current at 20 and 100 per cent relative humidity with 0.590-inch-gap blowout-type contactor on inductive load at 60,000 feet altitude

Load: Inductive C, Table I

Small-Gap Effect

The fact that a small gap of the order of one millimeter may have a higher interrupting ability than a larger gap is a well known phenomenon in a-c[5] arc interruption. Investigation of d-c interruption at small gaps has shown that a similar effect exists for direct current. Tests have shown that it is often possible to interrupt more current at either 125 volts or 250 volts, inductive or resistive load, with gaps about 0.005 inch or 0.010 inch than with gaps of 0.030 inch, especially at the higher altitudes. Practical considerations such as metal transfer or the building up of beads on tips due to arcing tend to limit the practical usefulness of this observed phenomenon.

Summary

This paper has presented quantitative information obtained on various direct voltages. It should be cautioned that the maximum interrupted currents given here should not be taken as the rating of a device. The actual rating is a certain fraction of the maximum and is dependent on factors such as factor of safety desired, tip life expectancy, maximum arcing time allowable, and so forth.

Based on data obtained so far, the arc-interruption problem at higher direct voltages appears quite feasible. Possible system voltages are three wire, 250 volt, or even 600 volt as far as arc interruption is concerned.

References

1. Why Tomorrow's Planes Will Have A-C Electrical Systems, T. B. Holliday. *Electrical Manufacturing*, July 1943, pages 88–90.

2. Peak Voltages With D-C Arc Interruption for Aircraft, V. E. Phillips, W. P. Mitchel. AIEE Transactions, volume 63, 1944, December section, pages 944–9.

3. Some Aspects of Inductance When Iron Is Present, L. T. Rader, E. C. Litscher. AIEE Transactions, volume 63, 1944, March section, pages 133–9.

4. The Mercury-Arc Cathode, C. G. Smith. *Physical Review*, volume 62, 1942, page 48.

5. Conduction of Electricity in Gases (book), J. Slepian. Lecture series, course 38, educational department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., April 1933.

Basic Considerations in the Selection of Generators and Batteries for Aircraft

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ELECTRIC systems as used on aircraft have developed from a more or less accessory status in older types of aircraft to a place of prominence in modern types of such importance that safety of operation is often dependent to a limited extent on electric-system reliability. Because of its importance the general requirements of the electric system must be conceived for a proposed new airplane design when the design is in an embryonic state. Preliminary electric-load requirements must be calculated and, as design changes are made, must be revised repeatedly in order to co-ordinate properly the electric-system design with the other design characteristics of the airplane.

During this process of development all controlling design features of the complete electric system should be considered under operating conditions approximating, as closely as possible, those which would be obtained during actual operation.

To facilitate the calculation of electric loads of an airplane in any design stage, and with accuracy consistent with good design, the method described in this paper has evolved from a study of this problem extending over a period of several years.

Method of Analyzing Aircraft Loads

To select a generating source adequate to meet the electric-load requirements of an airplane under applicable operating conditions requires a systematic survey of those conditions with the most severe load combinations that may be encountered. Thus, the number of generators and batteries and their ratings must be determined from the proposed system load calculations with due regard to the variable nature of the loads and to other influencing characteristics.

Since the preliminary studies of electric loads and the determination of required generating capacity for a proposed airplane present the greatest obstacles, the procedure discussed in this paper attempts to clarify this initial phase of design by considering the several factors contributing to the selection of generators and batteries; however, the method used for calculating loads is equally applicable to all stages of design with no variations nor exceptions.

The method of calculation as presented in this paper is founded on the fact that the established standard for rating electric machinery is based on internal heating of the machine; that is, the maximum output of the machine is limited by the maximum safe operating temperature for the type of insulation used. This applies to aircraft generators where the temperature rise of the generator is a function of its internal losses, the most important of which are windage, friction, hysteresis, eddy-current and copper losses. Of these, windage, friction, hysteresis, and eddy-current losses are functions of generator speed and field excitation. Copper losses are the products of the resistances of field and armature and the squares of their respective currents, and usually may be considered the principal source of heating when the generator is operated at 50 per cent rating or above. This is particularly true of present-day main engine generators which are cooled by air blast, since an increase in windage, friction, hysteresis and eddy-current losses due to higher generator speed is compensated by the increased blast resulting from the greater forward airplane speed which usually accompanies an increase in engine speed. Hence, by assuming that all, instead of only the major part, of the losses vary as the square of the armature current, this method of evaluating variable loads is slightly conservative.

The energy which produces heating of the generator conductors in any given period of time, T, is proportional to the integrated squares of instantaneous values of current as shown in the following equation:

$$W = R \int_{T_1}^{T_2} i^2 dt \text{ watt minutes}$$

where $T = T_2 - T_1$.

The steady value of current that would produce the same heating is equal to the square root of the mean square of the instantaneous currents integrated over the period T.

$$I_{rms} = \sqrt{\frac{W}{RT}} = \sqrt{\frac{1}{T}\int_{T_1}^{T_2} i^2 dt}$$

With the aid of this equation the load requirements for each operating condition of the airplane, and consequently the magnitude of the principal factor causing heating of the generators, may be determined, and it is of prime importance that all main operating conditions be analyzed to preclude the omission of any operating characteristics that might influence the generator capacity. The number of different operating conditions for a comprehensive analysis is dependent on the type of aircraft—land based or sea based—

Figure 1. Sample of chart used for tabulating aircraft electric loads

MODEL ____ ELECTRICAL POWER ANALYSIS CHART

____VOLT D.C. SYSTEM

NO.	ITEM	OPER. LOAD PER UNIT WATTS	OPER. LOAD PER UNIT AMPS.	TOTAL NO. OF UNITS	UNITS OPER. SIMULT.	SECONDS PER OPER.	TOTAL OPER./UNIT/HOUR	PART NUMBER	OPERATING CONDITIONS START COL.1. AMPS. 10 MIN.	STAND-BY COL.2. AMPS. 20 MIN.	TAKE-OFF COL.3. AMPS. 10 MIN.	LAND COL.4 AMPS. 10 MIN.	CRUISE COL.5. AMPS. 1 HOUR	NOTES.
1														1
2														2
3														3
4														4
5														5
85														6
86														7
87														8
88														9
89														10
90														11
91														12
92														13
93														14
94														15
95														16
96														96
97														97
98														98
99														99
100														100
	TOTALS.													
T1	MAXIMUM DEMAND (SIMULTANEOUS OPERATION- ALL APPLICABLE LOADS)													T1
T2	SHORT TIME PEAK CURRENT (R.M.S. FOR 2 MINUTES)													T2
T3	R.M.S. VALUE OF CURRENT (FOR REMAINDER OF PERIOD)													T3
T4	SHORT TIME PEAK PER GENERATOR – NORMAL OPERATION													T4
T5	R.M.S. CURRENT PER GENERATOR – NORMAL OPERATION													T5
T6	SHORT TIME PEAK PER GENERATOR – EMERGENCY OPERATION													T6
T7	R.M.S. CURRENT PER GENERATOR – EMERGENCY OPERATION													T7

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and on the use of the aircraft—military or commercial. Operating conditions may include loading, standby, starting, take-off, cruise, combat, landing, and others as may be applicable.

The first step in making the analysis consists of listing each item of utilization equipment on a chart similar to that shown in Figure 1. Adjacent to each item and in columns provided for the purpose are listed amperes per unit, total number of units, units operating simultaneously, seconds per operation, and total operations per unit per hour. Following this, the applicable operating conditions are

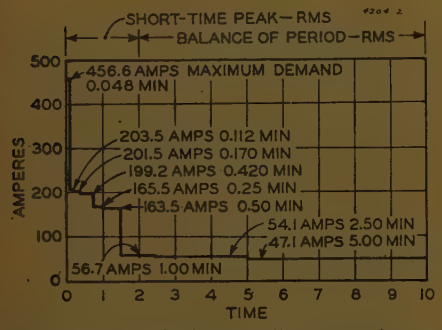


Figure 2. Block diagram illustrating the sequence of loads as used for calculating rms values of current

set up in columns, and the current for each item operating under each of the several conditions is noted.

Individual loads under each operating condition may be classified as continuous loads, which exist for the duration of the operating period considered, and noncontinuous loads, which exist for various portions of the operating period. Examples of these classifications are heating equipment, ventilating equipment, radio receivers, and inverters which are usually operated continuously, while items such as control-surface equipment, landing-gear retraction equipment, and engine starters are operated noncontinuously.

Calculations of maximum demand, short-time peak, and rms values of current for each of the several operating conditions with proper consideration of other influencing factors provide sufficient data to determine the number and capacity of generators and batteries for a given installation.

As indicated in Figure 2 and Table I, it is assumed in this method of calculation that for each operating condition all applicable loads begin simultaneously at the start of the period. This assumption serves to simplify calculations and its justification lies in the impracticability of attempting to estimate the actual order of occurrence of the various loads. This arbitrary sequence leads to pessimistic values of maximum demand and rms current but serves to compensate for neglecting motor-starting inrush currents. In addition to heating, the maximum demand must be considered because of the effect of high momentary currents upon commutation, regulation, and instantaneous torque demand on the generator drive.

Because of the relatively small margins of safety possible in aircraft generator design,[1] it is of great importance that adherence to design ratings be exercised. Many main engine d-c generators have a continuous full-load rating, a two-minute 50 per cent overload rating, and a five-second 100 per cent overload rating, whereas, most auxiliary power-plant generators have a continuous full-load rating and a five-minute 50 per cent overload rating. Thus, it is necessary when analyzing the operating conditions to evaluate the overloads for which there is a possibility of occurrence. This gives rise to the short-time peak current which is the rms value calculated over a two- or five-minute period, whichever is applicable.

The rms value computed for the balance of the period, after the short time peak has been found, is somewhat pessimistic as a basis for determining the continuous-duty capacity of the generator

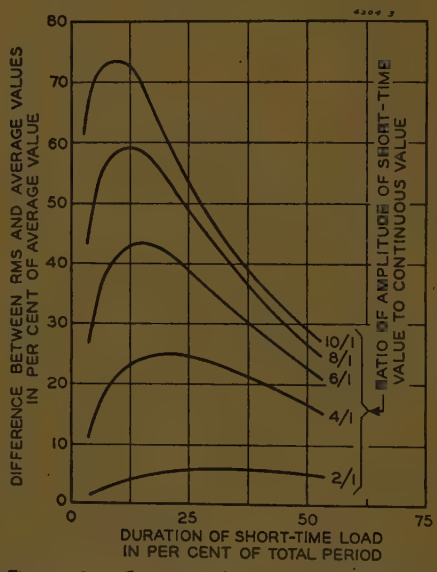


Figure 3. Curves showing percentage difference between rms and average values of current

supply, since the arbitrary sequence of the various loads tends toward larger rms values; but, on the other hand, the average value for the same period is optimistic. Thus, if both rms and average values are calculated, a bracket is established which extends from the most unfavorable to the most favorable sequence; hence, the actual operation must fall somewhere between the two extremes. In the absence of large part-time loads the rms and average values will approximate each other very closely, indicating that the sequence of application of the loads for this condition is of little importance; however, if the short-time loads are large their importance becomes of more concern. The difference between the rms and average values over an operating cycle is a function of the ratio of amplitudes of the short-time current and the continuous current, and also of the duration of the short-time current in per cent of a complete operating cycle. Figure 3 presents a family of curves showing the amount that the rms value exceeds the average value when a single short-time load is considered.

Table I and Figure 2 serve to illustrate the method of calculating maximum demand, short-time peak and rms currents for a typical operating condition.

Use of Load Calculations in Selection of Generators

With the aid of the most severe service requirements of the power supply, as made available by the calculations of maximum demand, short-time peak, and rms values of current for the several operating conditions, the active generator capacity may be determined. In addition to the active power requirements, other considerations which bear on the selection of the number and rating of generators are: emergency reserve capacity, excess capacity for future system growth, current peaks carried by batteries in low-voltage d-c systems, amount of monitoring that can be tolerated if necessary, altitude rating of generators, generator operating speed, and volt-ampere characteristics of the power-supply system.

Emergency Reserve Capacity

The capacity which should be provided for reserve depends on the likelihood of the generators or their controls becoming damaged or inoperative, and also depends on the importance of reliability of electric power supply, the reliability requirements in general being a function of size and power of the airplane. For almost all types of military aircraft reliability is usually of extreme importance from the

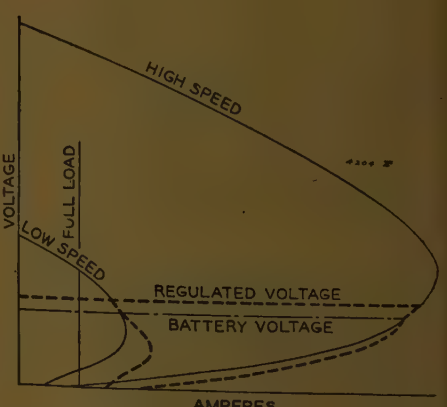


Figure 4. Aircraft-electric-system volt-ampere characteristics

—— Generator volt-ampere characteristic
---- System volt-ampere characteristic

standpoints of protection and safety of flight. For large commercial aircraft and especially those for which flight safety is dependent on electrically operated equipment, such as control surfaces, assurance of uninterrupted service is equally important, whereas, for small low-powered aircraft the loss of a generator normally would be of small consequence. Thus, the reserve capacity needed to insure reliability of electric power may vary from 0 to 100 per cent and is purely an arbitrary figure depending on the function of the airplane, flight safety requirements, and the probability of damage to the system.

Reserve capacity is normally provided in the form of more or larger generators, since the weight of batteries to supply power for an extended time is usually prohibitive. However, a complete evaluation of all factors sometimes will dictate the omission of part or all of the normal reserve generator capacity, and in such cases any generator failure will make it imperative to monitor the demand for power by limiting the operation of the utilization equipment. In case all the generator capacity is lost, failure to monitor will result in rapid discharge of the batteries with consequent loss of radio contact, electrically operated engine instruments, and possibly propeller pitch control. In case only part of the generating capacity is lost, the remaining units probably will be operating under an overload. In many ships not having a flight engineer, overload on the generator(s) is very likely to escape attention until the remaining units have overheated and failed. The importance of monitoring before this condition occurs is readily apparent and has led to serious consideration of the desirability of providing a warning whenever serious generator overheating has occurred. The addition of a built-in thermostat in every generator would provide a simple means of operating a red warning light.

Capacity Allowed for System Growth

The amount of capacity provided for future system growth is greatest in the initial stages of the system design, since, as the development of the airplane progresses, the initial allowance probably will be used. It has been common practice to provide from 10 to 25 per cent over and above the initial calculated requirements, depending on the conservatism of the design.

Current Peaks

The maximum demand current for any operating condition is often of a momentary nature which may be within the generator five-second rating previously mentioned. However, in the event that the maximum demand exceeds this rating in either amplitude or time, it is often desirable and advantageous to permit the batteries to supply the additional load on low-voltage systems. This is accomplished by co-ordinating properly the generator-voltage regulator combination to obtain a drooping voltage characteristic, as overload increases, of such nature as to reduce the generator voltage slightly below battery voltage at a predetermined overload. Any momentary current peaks are usually of such short duration that no provisions for added battery capacity are required, although this may not be true if the high-current peaks occur shortly after the batteries have undergone a period of high discharge. Such a condition might occur during take-off after main-engine starting has lowered battery capacity, or during landing if the engines are operated at low speed. Under the latter operating condition batteries would be required to supply a large portion of continuous loads in addition to peaks.

Monitoring

In some instances where maximum demand or short-time peak currents indicate the necessity for exceptionally large generator or battery capacities, it is convenient and practicable to regulate the sequence of operation of manually controlled motors and other loads in order to prevent excessive currents, thus permitting the use of lower rated generators and batteries.

Generator Altitude Ratings

Most main-engine generators maintain full-rated output up to 30,000-feet altitude, above which the output is an inverse function of altitude. The output of auxiliary-power-plant generators, however, is fundamentally dependent on the output of the internal combustion engine which is essentially an inverse function of altitude from sea level. Reduced auxiliary-power-plant ratings above sea level cause them to be very inefficient for altitude operation, and unless engine supercharging is provided, these units are used commonly only for ground supply and emergency reserve in flight, at reduced rating, for main-engine generators.

Generator Speed

To obtain rated output an aircraft generator must be operating within a given speed range with adequate voltage regulation. Thus, the possibility of generator speeds below the lower limit of rated speed must be given proper consideration for those operating conditions during which reduced speeds may occur. A condition of this type may become critical during landing, especially if the approach is made in a prolonged glide from high altitude, but in general would be relatively unimportant during ground operation with the main engines running.

System Volt-Ampere Characteristics

In order to prevent an unstable condition during maximum demand or short-time peak loading from which the system will not recover to normal voltage, the system volt-ampere characteristics must be co-ordinated with operating conditions. The following discussion applies to aircraft power systems consisting of

Table I. Sample of Tabulating and Calculating Loads

Continuous Loads (Current)	Noncontinuous Loads: Current	Noncontinuous Loads: Time
10.4	253.1	0.048
0.3	2.0	0.16
0.2	2.3	0.33
0.4	33.7	0.75
2.5	2.0	1.00
23.0	106.8	1.50
2.7	2.4	2.50
0.1	0.2	2.50
0.5	7.0	5.00
7.0		
47.1	409.5	

I	ΔI	t	It		I²t
456.6	253.1	0.048	21.92		10,008.7
203.5	2.0	0.112	22.79		4,637.8
201.5	2.3	0.17	34.26		6,903.4
199.2	33.7	0.42	83.66		16,665.1
165.5	2.0	0.25	41.38		6,848.4
163.5	106.8	0.50	81.75		13,366.1
56.7		0.50	28.35		1,607.4
		2.00 2	314.11	 2	60,036.9
			157.0 amperes (average)		30,018.4 (1/2) 173 amperes (rms)
56.7	2.6	0.50	28.35		1,607.4
54.1	7.0	2.50	135.25		7,317.0
47.1		5.00	235.50		11,092.0
		8.00 8	399.48	 8	20,014.4
			49.9 amperes (average)		2,501.8 (1/2) 50. amperes (rms)

generators, with or without batteries which, have over-all volt-ampere characteristic curves having a positive slope on the low-voltage end. At the lower end of the generator-design speed range the sytem stability is most critical, since aircraft generators usually are designed, so that the low-speed volt-ampere characteristic curve intersects the regulated voltage curve at approximately 150 per cent of normal rated current. Overload ratings of the generators apply for low-speed operation of this nature insofar as heating is concerned. At this low speed continued increases in load, in excess of the point of intersection of the two curves, result in a relatively sudden drop in the generated voltage; and further increases may reduce the voltage to a point of instability such that the generator excitation fails progressively, causing the generated voltage and load current to drop to small values. Under these conditions the generator is unable to recover voltage until the load has been removed from the system. At the upper end of the speed range the generators are capable of carrying several times normal rated current for periods of time governed almost entirely by temperature rise. However, the intersection of the high-speed volt-ampere characteristic curve and the regulated voltage curve is a limiting loading condition. Loads in excess of this value result in excitation failure and consequent simultaneous reduction of generated voltage and load current. Voltage recovery can be obtained only by removing the load from the system.

The use of batteries in parallel with low-voltage aircraft generators aids in alleviating this unstable condition by increasing the amount of load that may be carried before reaching the unstable portion of the curve. However, the use of enough battery capacity to eliminate completely the positive slope portions of the system characteristic curves is not practicable from a weight standpoint.

The general form of a volt-ampere characteristic curve of an aircraft electric system composed of generators and battery is shown in Figure 4. The effect of batteries in parallel with generators on the system characteristics is shown by the broken line. It will be noted that at low speed the addition of the battery improves the stability of the system appreciably by increasing the load over that which may be carried by generators alone as shown by the solid line.

Each of the previously noted factors must be given due consideration in selecting an economical and efficient generator supply of adequate capacity and reliability.

Selection of Batteries

An aircraft battery is an inefficient source of electric power compared with the generator, but its ability to deliver high short-time currents makes it highly desirable for aircraft application on low-voltage systems. In general, its primary purpose is for ground operations when generator power is not available, while its secondary purpose is for assisting the generators by carrying high peak currents, thereby improving the stability of low-voltage systems. Also, when operated in parallel with generators, the battery acts conveniently as a filter to reduce radio-noise interference.

Two methods for reducing the weights of battery installations have been employed successfully on certain types of airplanes. The first, applicable to land-based aircraft which operate from well-organized airports, utilizes a power supply from an external source connected into the airplane system through a built-in plug-type receptacle. In this type of installation power for engine starting and normal ground loads is supplied by the external source, and the airplane battery is only required to add stability to the system by carrying high-current peaks and to provide emergency power. It is apparent that military aircraft operating from advanced bases which are not equipped with external power facilities are not adaptable to a system of this type. The second method, applicable to medium and large airplanes either land- or sea-based, employs one or more auxiliary-engine generators which provide power for ground operations. In this type of installation the battery need only be large enough to start an auxiliary engine and provide a stabilizing influence on the system.

The following discussion on batteries is based on the assumption that neither of the aforementioned methods for augmenting ground operation power is used.

Engine Starting

Normally, the battery capacity required for a given aircraft installation is basically dependent on engine starting requirements, and as such should provide sufficient ampere-hour capacity for a limited series of starting attempts without completely discharging the battery. In order to assure adequate capacity for this purpose, several factors must be considered: the normal current demands for the size and type of engine starters used, and the effect on starter current and battery capacity of the ambient temperatures that will be encountered. Also, in applications where the battery is normally required to supply power for ground operations in addition to starting, an adequate margin should be allowed, so that engine-starting capacity will not be jeopardized.

Engine starters, although energized for relatively short periods of time, draw currents of exceptionally large magnitude, in some cases amounting to many hundred amperes, imposing severe demands on the battery. Even under the most ideal conditions obtainable, these high-discharge rates reduce the effective ampere-hour capacity of the battery. It should be noted that the discharge period of a battery is not inversely proportional to the current but approaches the inverse square of the current. For example, a fully charged 34-ampere-hour aircraft battery will discharge completely at 6.8 amperes in five hours; at 54 amperes it will take approximately 20 minutes, and at 144 amperes, five minutes, its effective capacity being 34, 18, and 12 ampere-hours, respectively. In addition to the effect of the normally large current demands of the starter on the battery capacity, the effect of low ambient temperature is twofold; first, because the viscosity of lubricants increases the load requirements to overcome friction, and, second, because the effective capacity of the battery drops with the temperature.[2]

Assistance to Generators

Improvement of system stability by assistance to generators in supplying high peak currents, is usually assured by the battery capacity determined from other considerations, since the peak demands which exceed generator capacity are usually of very limited duration.

Selection of Auxiliary-Power-Plant Generators

For some applications on large aircraft, where power requirements for ground operations are appreciably more than engine-starting requirements, it is found practicable to utilize auxiliary power plants in addition to main-engine generators. Because auxiliary power plants are normally inefficient at high altitudes, they are seldom used for this purpose except for special applications with supercharging facilities, although their use for medium- and low-altitude operation at reduced rating is not uncommon. The method previously discussed for determining required main-engine generator capacity is directly applicable to auxiliary-power-plant capacity with reservations regarding altitude operation as here noted.

General Applications

The method of computing aircraft electric loads, herein outlined for d-c systems, is equally applicable to a-c systems with proper consideration given to power factor and phase unbalance if on a multiphase system. It is also useful in making preliminary power studies on a wattage basis before system voltage has been established.

Although this method for determining aircraft electric loads may be applied generally to all types of aircraft, the establishment of a definite procedure for selecting the number and ratings of gen-

One Type of Rotary Magnetic Clutch and Its Associated Brake Used on Aircraft Electric Motors

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EVER since the first application of the electric motor on aircraft, attempts have been made to adapt clutches and braking devices to the motor and its power transmission system with varying degrees of success. Some current objectives are:

1. To limit overtravel of and incorporate accurate positioning in a mechanism, such as landing gears, cowl, or wing flaps of an airplane.

2. To limit the application of load to a motor until such time as the motor has come up to speed.

3. To allow quick reversing of a gear train.

4. To permit temporary disengagement of load at a predetermined torque on the motor shaft (slipping clutch).

With recent accelerated development of aircraft, weight has become a major factor influencing the design of all accessories of such craft. Consequently, the lightweight high-torque high-speed electric motors developed for airplanes have challenged anew the ability of clutch and brake designers.

It is not proposed in this paper to discuss all of the numerous types of nor applications for brakes and clutches, but to consider only one type of clutch and a brake used in conjunction with it. The brake, a design of which is carried out later in this paper, is used to hold a load, through the associated power transmission system after the motor power has been cut off. Another type applies to a mechanism that may be operated manually during an emergency. This necessitates a mechanism which has a free driven disk member.

The clutch considered employs an application of the d-c short-stroke clapper electromagnet. Figure 1A shows the driven disk of the clutch which is made from one solid piece of steel and has the motor-drive pinion cut on its extremity. It is free to move axially through its bearings. Figure 1B is a view of the driving disk. This disk is keyed to the armature shaft (Figure 2) and rotates with it. The stationary clutch coil is wound around a core which forms a part of the inner housing of the driving disk. Figure 2 illustrates a section of the assembled clutch and brake with dimensions indicated. Dotted lines trace the paths of flux. This clutch has been used on a 100-watt 9,000-rpm intermittent-duty motor, developing 0.6- to 1.0-inch-pound torque. It will be noted from reference to Figure 1A or Figure 2 that there is a ring of nonmagnetic material dividing the face of the driving disk into two equal areas. The purpose of this ring is to direct the flux into the driven disk instead of allowing it to shunt across the face of the driving disk. Braking is accomplished mechanically by forcing the brake face of the driven disk (face opposite the clutch engaging one) against a ring of braking material fastened to the housing.

Different materials, including metals, have been used for both braking and slipping clutches. The slipping clutch has met with only fair success on high-speed applications. This, in no small part, is due to a changing coefficient of friction, which is in turn some function of the number of slippages. Steel upon steel has an initial coefficient of friction varying from 0.15 to 0.25. Positive engagement of the clutch allows no slippage; consequently, most clutch coils are in series with the armature. From a standpoint of economy, reliability, and ease of installation, material having a coefficient of friction from 0.2 to 0.4 is the most suitable for braking. Obviously, with the clutch and brake combination just the low-inertia driven disk is braked,

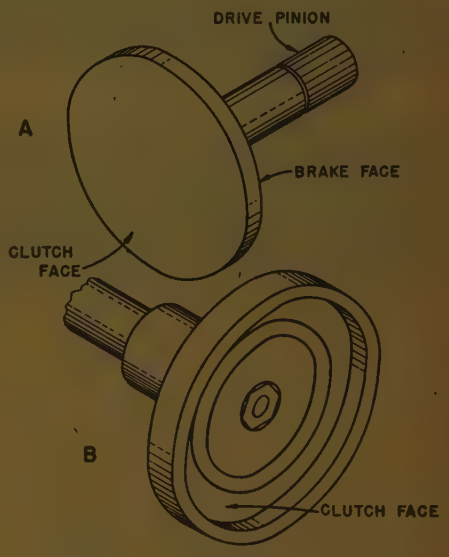


Figure 1. Rotating parts of clutch

Table I. Absolute Permeabilities for Varying Cross Section

Location	l	A	B	NI/In	μ
l_1	0.0	0.143	84	10.1	8.280
l_2	0.1	0.164	73	8.1	9.060
l_3	0.2	0.185	65	6.5	10.000
l_4	0.3	0.212	57	5.4	10.500
l_5	0.411	0.230	52	5.1	10.200

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erators and batteries is prohibited by the variable nature of the other influencing factors.

Conclusions

The ever increasing electric applications in aircraft add to the desirability of an adequate flexible method of evaluating loads under practical operating conditions. The method as described in this paper has proved very satisfactory and reliable for this purpose, and when used in conjunction with the other influencing factors it serves well as a basis for selection of generators and batteries.

References

1. DESIGN RELATIONSHIPS FOR D-C GENERATORS FOR USE IN AIRCRAFT, S. R. Bergman. AIEE TRANSACTIONS, volume 62, 1943, October section, pages 613–16.

2. AIRCRAFT ELECTRICAL ENGINEERING (book), Randolph Matson. McGraw-Hill Book Company, Inc., New York, N. Y., 1943, pages 136–9.

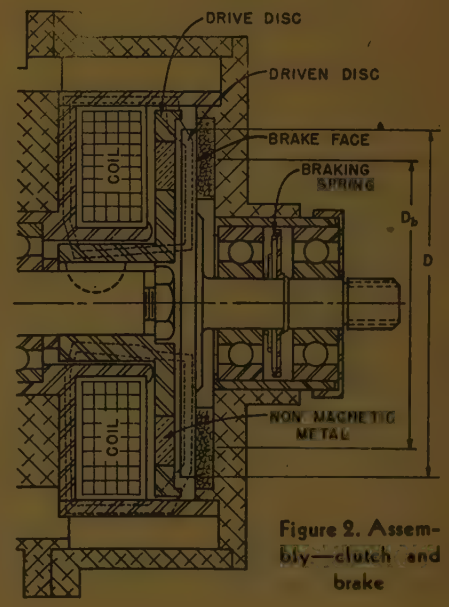


Figure 2. Assembly—clutch and brake

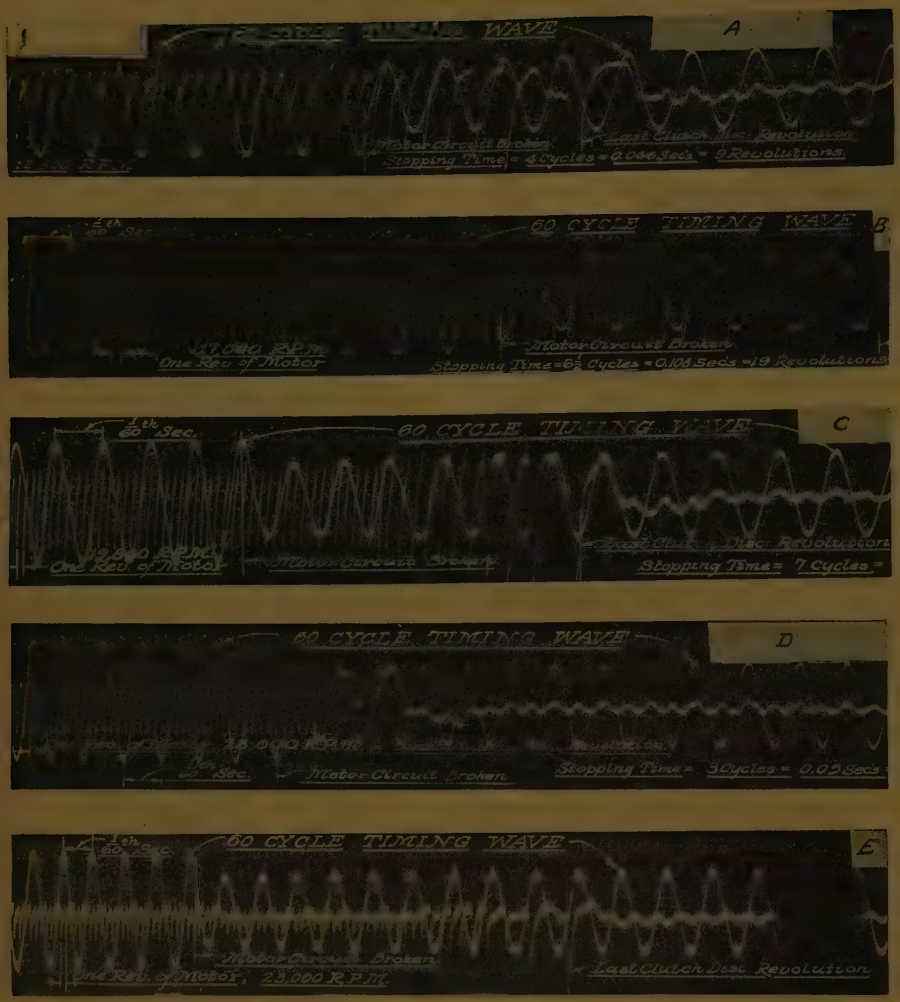


Figure 3. Performance of brake

A. Motor data—450 watts; 24 volts; 30 amperes; weight, 5.6 pounds
B. Motor data—100 watts; 24 volts; 8 amperes; weight, 3.25 pounds
C. Motor data—75 watts; 24 volts; 8 amperes; weight, 1.9 pounds
D. Motor data—30 watts; 24 volts; 4 amperes; weight, 1 pound
E. Motor data—30 watts; 24 volts; 4 amperes; weight, 1 pound

while the relatively heavy armature is allowed to coast to a standstill.

Small 24-volt d-c motors with spring-actuated brakes in the output range of 0.02–0.08-inch-pound torque have been reversed as many as 18 times per minute on plugging duty without failure over 60-hour intervals.

The braking performance of a group of high-speed motors ranging in output from 35 to 500 watts is shown in the oscillograms of Figure 3. The accompanying data inscribed upon the prints make the figure self-explanatory.

With the mushroom growth of war activities, this clutch, as is true of so many other new developments, like Topsy, "was not born, but just grew." The following sections entitled "Analysis of the Magnetic Circuit," and "Detailed Design," which have been explained in the foregoing paragraphs, are attempts to pause and consider what is happening in the devices. Concepts and design will be presented straightway with no further explanations and with the hope that they will form a background for further development.

Analysis of the Magnetic Circuit

Figure 4 comprises a part of Figure 2 (assembly) and is used to illustrate this analysis. The path of the magnetic circuit has been divided into five parts, designated as A, B, C, D, and E for the purpose of calculating a magnetization curve. It was developed by assuming different values of flux, then by calculating the ampere turns necessary to send each value through the various parts of the circuit. This curve is shown in comparison with a test obtained by means of a ballistic galvanometer and exploring coils in Figure 5.

Table II. Ampere Turns for Individual Parts of Clutch

	Lines of Flux				
	4,000	8,000	12,000	16,000	20,000
Part A	1.5	2.0	3.0	6	19.5
Part B	0.5	1.5	28.0	52	78.0
Part C	2.0	3.5	6.0	12	20.0
Part D	1.5	2.0	2.8	6	19.5
Part E	2.5	3.0	4.0	5	7.0
Total	8.0	12.0	43.8	81	144.0

Parts C and E have uniform cross-sectional areas. Parts A and D have varying cross-sectional areas. The procedure used in calculating the ampere turns required for a magnetic path with a varying cross section best can be explained by considering part D with an assumed total flux. This flux will be chosen as 12,000 lines, it being approximately the flux required to hold the clutch plates together when the motor is transmitting rated torque. Cross-sectional areas are determined for various locations; the corresponding flux densities are obtained by dividing the total flux by the areas; then from a magnetization curve of the material the ampere turns per inch are read.

The ratio of flux density per square inch to ampere turns per inch is the absolute permeability at the various cross sections. For a total flux of 12,000 maxwells Table I is made, in which NI is ampere turns, μ is absolute permeability, A is area in square inches, B is flux density in kilolines per square inch, and l is distance from the inside of the disk in inches.

If a graph of the absolute permeabilities at various locations is constructed the average absolute permeability can be determined by measuring the area and dividing by the length of the magnetic path. Such a graph is presented in Figure 6. The average permeability is found to be 9.745. Now from another graph of ampere turns per inch versus permeability, the ampere turns per inch are found to be 6.8. This value multiplied by the total length of 0.411 inch gives us a total of 2.8 ampere turns for part D. A summation of the calculations is shown in Table II. Adding to the ampere turns

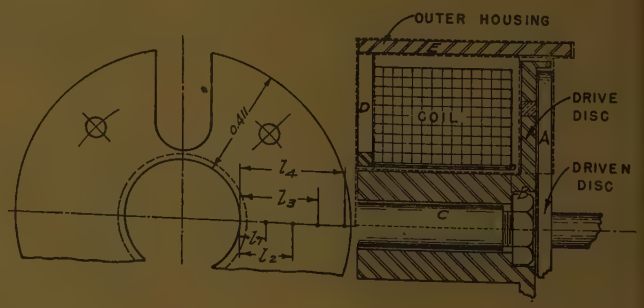


Figure 4. Sections of flux path

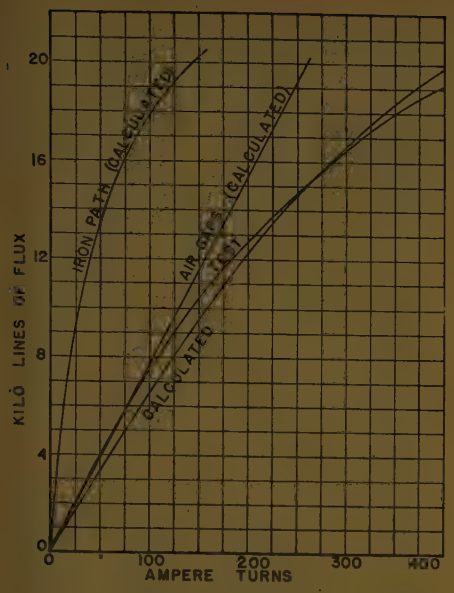


Figure 5. Magnetization curves of clutch

shown in Table II, those necessary to send the assumed values of flux through the air gaps, complete data for the magnetization curve are obtained, as presented in Table III, from which the curves in Figure 5 are drawn. Part *B* has a section in the drive disk which becomes saturated when approximately 8,000 lines of flux are passing through the circuit. Beyond this value some of the flux crosses the air gap into the nut on the shaft.

There are two air gaps to consider in a general analysis. One is the gap between the inner housing and the drive disk or between parts *E* and *A*. The other is between the drive-disk hub and the core or between parts *C* and *D*.

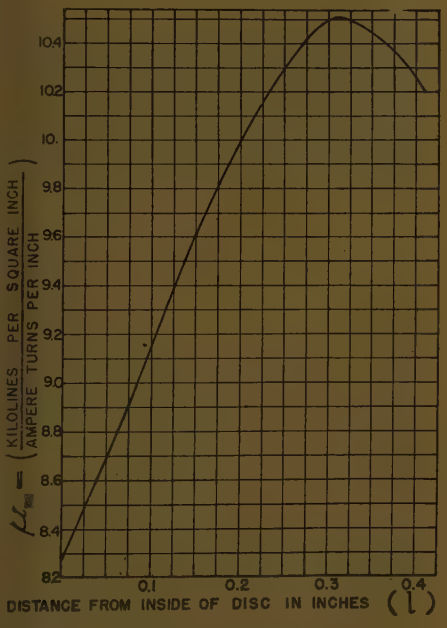


Figure 6. Absolute permeabilities of clutch-coil mounting plate

Table III. Summary of All Data

Lines of Flux	4,000	8,000	12,000	16,000	20,000
Ampere turns, iron path (calculated)	8	12	46	76	144
Ampere turns, air gaps (calculated)	52	104	156	208	260
Ampere turns, total (calculated)	60	116	202	284	404
Ampere turns (test)	50	110	185	285	440

Calculated flux densities expressed in kilolines per square inch in the designated parts with a total flux equal to 12,000 maxwells are as follows:

A	B	C	D	E
52 to 84	79	80	52 to 84	44

Detailed Design

The dimensions as indicated in Figure 2 will be used.

Moment of inertia of driven disk (comprised of two sections, the moments of which are computed separately):

$$V=\frac{D^2\pi}{4}\times\text{thickness}$$

$W=0.28V$ (for steel)

$I={}^1/_2\times{}^1/_{12}\times W/g$ pound-inch-seconds^2

$=M/24$

Kinetic Energy

$$KE={}^1/_2I\omega^2={}^1/_2I\times\left(\frac{2\pi N}{60}\right)^2=\frac{I\pi^2N^2}{1{,}800}\text{ inch-pounds}$$

Determination of brake spring force to stop disk in R revolutions.

A point on the average braking circumference of the driven disk moves RD_b inches.

$$F_1=\frac{KE}{RD_b\pi}\text{ pounds}$$

$$F_2=\frac{F_1}{C_b}\text{ pounds}$$

Disengaging Time

Axial acceleration at disengagement due to $F_2=\frac{F_2}{M}$ inch-seconds.

$$t=\sqrt{\frac{2d}{F_2/M}}=\sqrt{\frac{2dm}{F_2}}$$

Disengaging Slip

$$S=\frac{tN}{60}$$

Determination of flux required to pull driven disk into engagement with driving disk at no load:

$$F_2=\frac{B^2A}{72\times10^6}$$

or

$$B=\sqrt{F_2\times72\times\frac{10^6}{A}}$$

Likewise, the flux required to hold the disks together at any load is calculated as follows:

$$F_3=\frac{T_c}{C_c}$$

$$F_3+F_2=\frac{B^2A}{72\times10^6}$$

$$B=\sqrt{\frac{(F_3+F_2)72\times10^6}{A}}$$

Notation

A, area of clutch face of driving disk, $D^2\pi/4$ square inches
B, flux density, maxwells per square inch
C_c, coefficient of friction, clutch
C_b, coefficient of friction, brake
D, diameter of driven disk, inches
D_b, diameter of braking-ring material, average, inches
d, clutch clearance (between disks) maximum, inches
F, frictional force on brake, pounds
F_2, spring force, pounds
F_3, clutch force required to transmit torque, pounds
g, acceleration due to gravity, 32.2 feet per second per second
I, moment of inertia, pound-inches–seconds^2
KE, kinetic energy, foot-pounds
M, mass, pounds
N, motor speed, rpm
S, disengaging slip, revolutions
R, revolutions
t, disengaging time, seconds
T_c, torque to be transmitted by clutch with any applied load, pound-inches
V, volume, cubic inches
ω, angular velocity, radians per second
W, weight, pounds

Conclusion

To fill a long-standing need for clutch and brake development for electric motors, and because this need has been augmented and specialized by our current war-airplane manufacturing program, it seems imperative that the magnetic circuit, flux densities, mechanical and electrical forces, and in addition, the best magnetic coil dimensions be studied in order to perfect the most efficient design of this type mechanism.

It is hoped that this paper will be instrumental in breaking the ground for future development along these lines.

Electric-Wiring Installations in British Aircraft

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NONMEMBER AIEE

Synopsis: This paper deals with the growth of aircraft electric-wiring installations consequent upon the increasing use of electrical equipment in combat aircraft of all types. A brief description is given of the various proprietary and prefabricated systems of wiring in current use in British aircraft. It concludes with a description of the principles of the standard system now in an advanced state of development which will be applied to all new types of British aircraft now in the design stage.

THE increasing use of electrical equipment in modern aircraft has in its train focused attention on the distribution system. The major items of electrical equipment in British aircraft generally are supplied to the aircraft constructor as government-approved items. It will be obvious that their correct functioning depends upon the wiring installation which serves them. The size, shape, and equipment layout of an airplane does not lend itself readily to an orderly and well-spaced electric installation. The concentration into certain relatively small areas, peculiar in shape, of a large number of electrical items concerned with navigation, control, offense, and defense, is inevitable and constitutes a major problem for the designer. It is perhaps difficult to appreciate that in a modern four-engined bomber, some $4^1/_2$ miles of single-core electric cable would be required and in a fully equipped two-engined aircraft, $2^1/_2$ miles would be used. The problem of accommodation and efficient installation of this amount of wiring demands careful thought and planning. It is unfortunate that even up to the present day too little thought has been given to providing an orderly and well-engineered wiring installation and to ensuring well-protected cable routes. The impression still persists in some quarters that electric cables, being flexible, can be moved and disturbed at will to give pride of place to other more clamorous demands. This has resulted in wiring being "slung in" and is responsible for a high percentage of electric failures directly attributable to faulty location of wiring and equipment in situations where ingress of moisture, mechanical damage by air crew, and deterioration by extreme heat all have made their contributions. The Ministry of Aircraft Production, Directorate of Technical Development, now has specified design requirements in this connection which should mitigate, if not entirely overcome, such trouble in future installations.

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Elementary Wiring Installations

It was formerly the practice in British aircraft carrying a relatively small amount of electrical equipment, to employ a simple point-to-point system of wiring using single-core cables and the form of terminal block illustrated in Figure 1. This terminal block consists of a bakelite moulding having metal inserts carrying a terminal screw and captive washer. The terminal nests are of the shrouded type, enabling the bared end of the cable to be formed around the terminal screw thus preventing the straying of strands of wire to adjacent points; a simple cover completes the assembly. Such blocks are provided in a useful variety of ways and capacities.

With the growth of the electric installation, this system tends to become cumbrous, involving practically the whole of the preparation and installation work being done on the actual aircraft during construction. With the requirement for rapid production to meet the needs of a growing air force, consideration was given to increased assembly of the unit principle, as employed in the Breeze system, into British aircraft. Subsequently other systems similar in principle were evolved and provided not only for accelerated initial production, but also for more rapid repair and replacement of damaged sections by repair and maintenance personnel.



Figure 1. Typical terminal block (old pattern)

Description of Various Wiring Systems Now in Use

The Breeze System

This system involves the use of branching cable conduits having their ends terminated by metal plugs, which upon assembly into the aircraft are inserted into sockets carried on panels or junction boxes previously installed as completely wired assemblies.



Figure 2. Breeze plug and socket for 7-, 19-, and 37-ampere conductors

The conditions which the designers of this system set out to meet were:

1. Minimum installation time to avoid congestion of personnel on the aircraft during construction.
2. Sectionalization to provide transport joints with quick matching up of aircraft sections.
3. Ease of isolation for maintenance testing.
4. Suitability for mass production by low-standard labor.

Typical Breeze plugs and sockets are shown in Figures 2 and 3, and the principle illustrated is identical throughout the range of sizes. The external shell of aluminum alloy carries bakelite mouldings which house the plug pins or socket inserts. The socket inserts are pressed from sheet brass to the required shape and have the necessary resilience to ensure adequate electric contact under vibration. The plug pins are turned from brass rod or alternatively are "bumped up" from brass wire.

The earlier Breeze installations em-

ployed flexible metallic conduits but the present practice is to use polyvinyl-chloride conduits except in the few cases where local screening is a requirement. General screening is achieved by suppression at the source of interference.

Figures 4 and 5 show a Breeze-wired main electrical panel for a heavy bomber and a typical Breeze junction box, respectively.

The original Breeze installations employed soldered or screwed-down connections throughout, but more recent installations have incorporated the form of quick release connector block shown on Figure 6. This connector incorporates a solderless (crimped) spade terminal at the cable end.

The Lucas Loom System

This system was developed to provide a lightweight system of wiring components and prefabricated looms primarily for installation in fighter aircraft. It differs from the Breeze system in that conduits normally are not used, as the cables are whipped or bound into looms without additional protection. Quick-release connector blocks take the place of plugs and sockets, except in situations where a plug and socket is the more appropriate form of connector to employ. The connector block and other details are shown in Figure 7. The connector block consists of a bakelite moulding designed as a three-way junction. The actual electrical connections take the form of a metal socket split along its longitudinal axis and having pressure maintained by a central spring-loaded screw. Sockets may be either of single-tier or double-tier type. A series of connector blocks is built up as required into a bank by means of a tie rod as illustrated. Backing strips and covers are provided to prevent accidental short-circuiting of connections, and fixing feet are added as necessary.



Figure 3. Breeze plug and socket for 64-ampere conductor

The prepared ends of incoming cables are soldered to metal nipples and these nipples act as the plug for insertion into the split sockets.

The procedure adopted for installation of this system into aircraft is as follows. The cable looms for each section of the aircraft (see typical examples in Figures 8 and 9) are suitably whipped and attached to plywood transport boards. These boards are carried into the aircraft, the lashings cut, and the loom run out into position and cleated to prepared positions on the air frame. The nipples are then inserted into the sockets on the connector blocks which previously have been installed in selected positions. Connector blocks and cable ends are identified appropriately to insure correct assembly. Final tightening of the central connector screw on the block ensures a reliable electrical junction.

The Strip System

The rapid increase in aircraft production imposed an increasing strain on the already overloaded facilities available for the production of the Breeze and Lucas systems. The principal difficulty was in the number of machined parts required and the making up of the conduits or looms. As a means of overcoming these difficulties and to cater to the requirements of new types of aircraft coming into production, the strip system was introduced. The feature of this system was that it relied more on pressed than on turned parts and employed multicore cables instead of conduits or looms. The name "strip system" was derived from the comprehensive use of a ten-way connector of strip form.

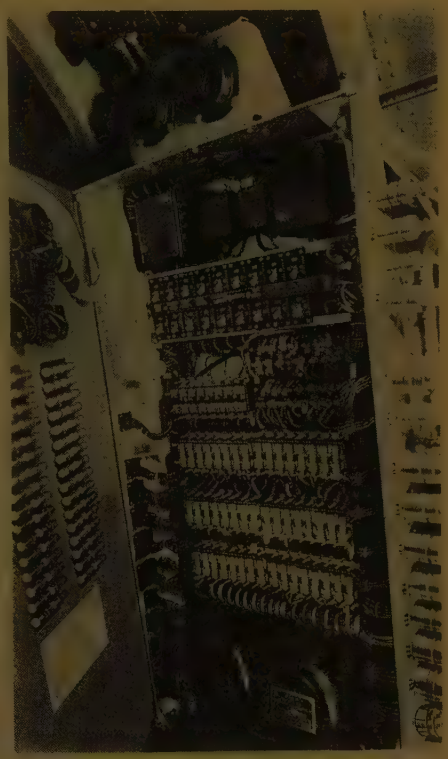


Figure 4. Breeze panel, typical aircraft assembly



Figure 5. Typical breeze junction box

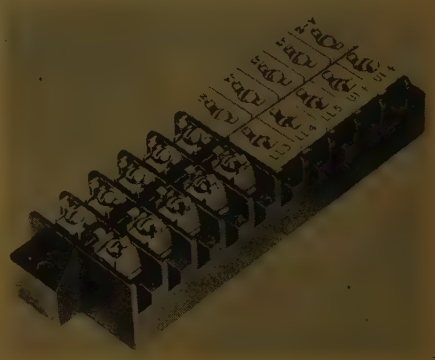


Figure 6. Breeze 20-way terminal block

The ten-way strip connector consists of pressed-copper contacts, silver-plated and gold-flashed, attached securely to a laminated bakelite strip. The contacts are located in slots in the bakelite strip (see item 9 of Figure 10). Between the interior surfaces of the contacts and nested in the central holes in the bakelite strip are stainless-steel springs to provide necessary contact resilience. Two assembly holes—one square and the other round—are provided in the bakelite strip. The prepared ends of the ten-core cable are soldered to the contact-strip extremities.

Five sizes of ten-core cables are provided for main-trunk lines and comprise an intelligent arrangement of 4-, 7-, and 19-ampere cores; rubber dielectric is employed and the exterior sheathing is polyvinyl-chloride.

Item 1 of Figure 10 shows a made-up cable assembly complete with its identification markers and the necessary rubber grommet for attachment to a distribution or junction box.

The distribution and junction boxes vary only in that the former carries fuses whereas the latter is purely a "break" box. Figure 11 shows typical junction boxes for this system. The larger box has a bakelite base, teak sides, metal slides for accommodation of cable grommets, and a metal cover. The small box is an aluminum die casting. Covers of all boxes are provided with double gauze-protected breathing holes.

On installation in the aircraft, the strip connectors with cable attached are

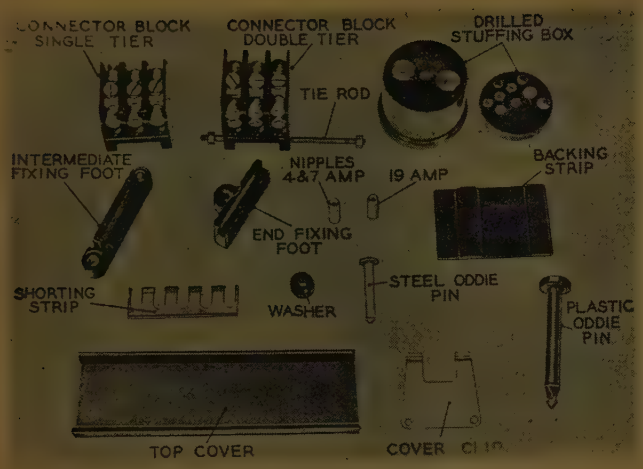


Figure 7 (left). Lucas loom-system components

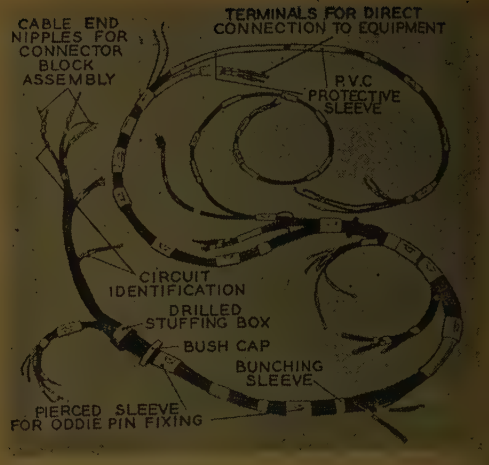


Figure 8 (right). Typical Lucas loom

assembled on pillars provided in the distribution or junction boxes in accordance with a few elementary rules and instructions which appear on the box cover (see Figure 12). Assembly order therefore is identified fully. The height of the pillars is sufficient to accommodate a maximum number of five strips connector—normally two sets of incoming and outgoing cables and a T-shaped cable. The connector strips have their short ends painted either "red" or "white" and like colors always must be in physical contact and insulated from unlike colors. The action of screwing down ensures efficient electrical connection between connector strips. When less than the maximum number of five strips are assembled, the space remaining is made up by the use of bakelite backing strips (see item 4 of Figure 10). Local single-core cables are attached to the strip connectors in similar manner to the main ten-core cables as shown in item 10 of Figure 10.

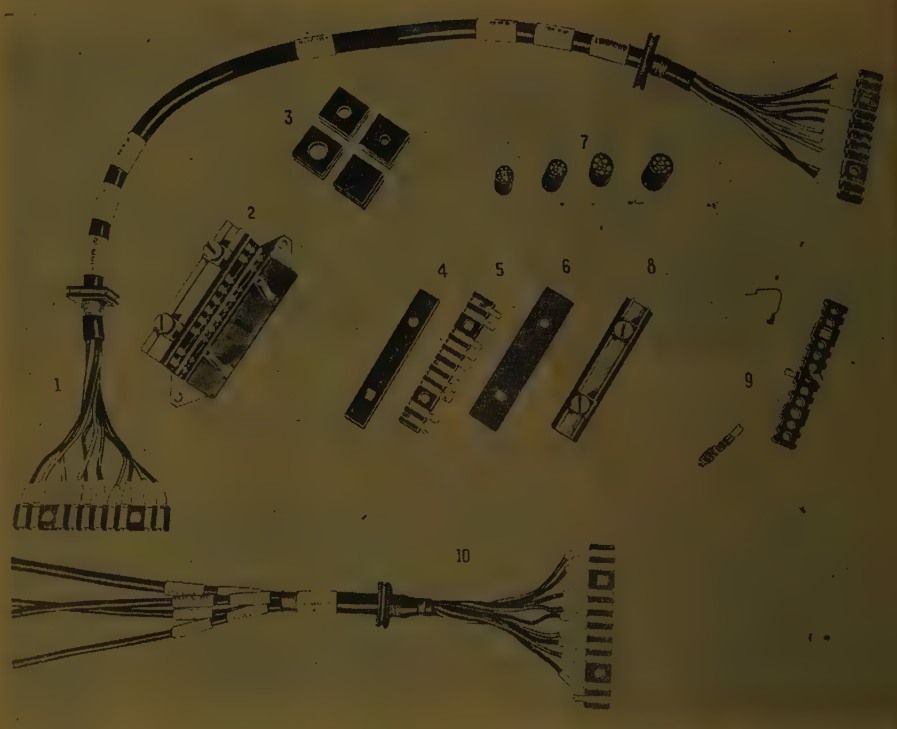
The principle of application of the strip system is similar to that employed for the Breeze and Lucas systems. Looms of multicore and local cables complete with their strip connectors all appropriately identified are brought into the aircraft, and cleated into position on the airframe, and finally the strip connectors are assembled in the distribution or junction boxes previously installed.

Standard spare lengths of ten-core cable assemblies are available for replacement and repair purposes.

Figure 9. Typical Lucas loom installed on aircraft



Figure 10 (below). Strip-system components



Development of the Standard System

The wiring systems just described have given reasonably good service under wartime conditions, but the existence of at least three distinct and varying systems of wiring presented difficulties of maintenance and storekeeping which can well be imagined. Consideration therefore has been given to the development of a standard system of wiring which could be applied comprehensively to all new types of British aircraft. The existing systems were examined critically to ascertain whether any of them could be adopted as the approved standard, but it was found that each exhibited weaknesses of greater or lesser degree. The Royal Air Force and Fleet Air Arm representatives were consulted on their experience of actual field conditions and expressed a strong preference for stand

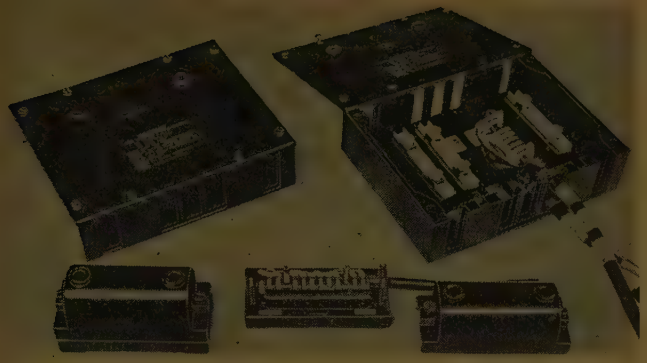


Figure 11. Typical strip-system junction boxes

ardization of a system akin to the Lucas loom system but with improvements in insulation value, resistance to corrosion, and maintenance facilities. This recommendation formed the basis on which development proceeded under the guidance of a committee representative of the Society of British Aircraft Constructors, the electrical industry, and the Directorate of Technical Development of the Ministry of Aircraft Production. The development of the standard system and components is not yet completed in every detail, but the following is a description of the main principles and component parts:

1. The main points which needed to be borne in mind during this development were: simplicity of engineering, ease of production, flexibility to accommodate modifications, economy of weight and space, efficiency under all conditions of operations. The system is based on the use of single-core cables for general wiring, the reduction to the minimum of junctions and joints, and the use, as generally as possible where breaks are essential, of efficient connector blocks.

2. Connector blocks for cables up to and including 37-amperes capacity are of the "quick-release" form. A typical 19-ampere block is shown at Figure 13. It will be noted that this block conforms closely to the principle of the Lucas block and comprises a bakelite moulding dimensionally designed to give the maximum creepage path consistent with reasonable size and weight. Apertures right through the block ensure free circulation of air, drainage of deposited moisture, and rapid drying. Molded-in inserts are provided for the reception of socket assemblies, either single- or double-tier as illustrated. The sockets are split along the longitudinal axis and pressure is controlled by a spring-loaded screw. All metal parts employed are noncorrodible.

3. The 19-ampere blocks will be available in 2-, 3-, 5-, and 15-ways and covers are provided for use when required. Identical in principle, the 37-ampere blocks are slightly larger and are available in two- and three-ways.

4. Cable ferrules similar to the Lucas nipples are attached to the cable ends by being crimped to both the cable core and the dielectric, providing thereby a rigid bond and support for the joint.

5. For cables of 64-ampere capacity and larger, a heavy-duty connector block will be provided. This consists of a bakelite moulding having a molded-in central pillar and a recess for the reception of the cables. Pressed cable lugs of the palm type are employed. These are assembled as required on the central pillar and securely clamped by a lock nut. There are no multiple combinations of this block. Attachment of lugs to the cable core normally is done by clamping, but soldering is also demanded and preferred.

6. Suitable hand crimping tools have been designed for attaching ferrules and lugs, but it obviously will be necessary to consider bench and power-operated tools when full production of the system arises.

7. Junction boxes will not be used in connection with the standard system in situations where the aircraft structure or a cover of simpler form than a box (for example, a canvas cover) provides sufficient protection for banks of connector blocks. For exposed positions, however, or in situations where mechanical damage is possible, standard boxes will be provided which are designed to be splashproof and capable of draining away any accumulation of moisture due to internal sweating.

8. Fuses generally will be mounted on panels and designs are in hand for combined fuse holder and connector blocks to cover the standard fuse range up to and including 100 amperes. Above that capacity it is probable that circuit breakers of the trip-free type will be used. A standard identification scheme also has been produced concurrently with the foregoing development and will become an integral part of the standard system.

9. Cable looms will be prepared external to the aircraft and installed in similar manner to that outlined for the Lucas system. The electrical installations for new types of aircraft now being designed will employ the standard system.

Cables

Hitherto on British aircraft, there has been a general use of "cel"-type cables. These cables employ cellulose-lacquered cotton cambric as the external sheath and rubber as the dielectric. In the past "cel" cables have given reasonably good service, but there has been difficulty under wartime production conditions in ensuring the necessary high-grade finish of the sheath to resist attack by gasoline, oil, and hydraulic fluid. There is now a tendency to employ polyvinylchloride as the external sheath in view of its high resistance to action by these fluids.

A further development now in the initial stages has been occasioned by the gradually deteriorating quality of the rubber permitted for use as a dielectric. From this development sample cables employing polythene as the dielectric have been produced and appear to be



Figure 12 (left). Typical strip-system aircraft assembly



Figure 13 (right). "Standard" system five-way: 19-ampere connector block

Airline Aircraft Electric-Equipment Maintenance

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ASSOCIATE AIEE

"ANNOUNCING the departure of trip 23, Nashville–Memphis–Phoenix–Los Angeles, leaving at gate 6, all aboard, please." In a matter of minutes after this announcement an airliner departs on schedule to carry some 5,000 pounds of passengers and cargo over rivers, mountains, and cities and to deposit them safely at the destination. Another routine flight has been made possible by the combined efforts of a small army of airline employees.

Electrical engineers will be interested to know that a large part of the success of the trip is the result of their efforts in the development of reliable aircraft electric equipment. So dependent is the present-day air transport upon its electrical equipment that without it airline operation as it is known today would be quite impossible.

The foregoing statement is not to be misconstrued to mean that an aircraft's electrical system is essential for successful flight, since in aeronautical terminology, a distinction is made between the engine electric-accessory equipment necessary for engine operation, namely, magnetos, booster coils, ignition wiring, and so forth, and the remainder of the electric system such as the generators, instruments, lights, radio, and similar equipment. Although it is entirely possible to take off and fly an airplane without the aid of any of the latter equipment, long-distance flights or even flights extending beyond the visibility of the airport would be extremely hazardous except under the very best conditions of daylight flight, with ceiling and visibility unlimited weather, and freedom from icing conditions.

It is the purpose of this paper to present the airline operator's problem of insuring reliable operation of the aircraft electric equipment. This problem will be presented from the standpoint of operation of American Airlines' fleet of 50 Douglas *DC*-3 transport airplanes, the conventional 21-passenger low-wing all-metal monoplane. The following statistics will give some idea of the magnitude of the problem. During the year of 1943 to 1944, some 449,556,248 passenger miles were flown with an average flying time of 10.2 hours per day per airplane. This flying was done over 8,369 miles of routes extending from New York to California and from Canada to Mexico and serving some 45 cities in that area.

A glance at the organization chart of American Airlines' maintenance department (Figure 1) will show the arrangement which has been found necessary to accomplish the job of maintaining a fleet of airplanes. It is impossible to discuss the problem of aircraft electric-equipment maintenance without considering the entire system, inasmuch as the basic maintenance procedures are formulated from the operating experience with all components of the airplane and are carefully planned to produce the maximum effect with the minimum cost. The following is a thumbnail sketch of the maintenance system and procedures.

Briefly, there are four stations designated as base stations, one each at Burbank, Calif.; Fort Worth, Tex.; Chicago, Ill.; and La Guardia Field, New York, N. Y. In addition all the overhaul facilities: shops, engineering, and administrative departments are located at La Guardia Field. All other stations in the system are commonly referred to as line stations.

The chief responsibility of the maintenance department is to keep the airplanes airworthy. The Civil Aeronautics Administration has outlined in some detail in the regulations the definition of airworthiness as it applies to all parts of the airplane. A unit of equipment designated as airworthy obviously must have a certain degree of reliability. The reliability of an article can be controlled to some extent by the application of inspection and overhaul procedures; however, the application of such procedures increases the cost of operation and some economic and workable balance must be obtained.

The airline's yardstick of service is the hour. One hour of service on a part means that it has flown for that period of time clocked from wheels off to wheels down and represents approximately 155

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Table I. Electric-Equipment Overhaul Time Table

Equipment	Overhaul Period, Normal	Overhaul Period, Maximum
Generator control box	400	425
Landing-light bulbs	400	460
Starter motor	Every engine change*	
Propeller-deicer pump motors	Every engine change	
Generators	Every engine change	
Tachometer indicators	Every engine change	
Carburetor-temperature bulb	Every engine change	
Oil-temperature bulb	Every engine change	
Thermocouple washer	Every engine change	
Exhaust-analysis cell	Second engine change	
Tachometer magneto	Second engine change	
Voltammeter	Second engine change	
Pitot-heater ammeter	Third engine change	
Fuel-quantity indicator	Third engine change	
Exhaust-gas-analyzer indicator	Fourth engine change	
Air-temperature gauges	Sixth engine change	
Carburetor-temperature gauges	Sixth engine change	
Cylinder-head-temperature gauges	Sixth engine change	
Fuel-quantity tank units	Sixth engine change	
Oil-temperature gauges	Sixth engine change	
Synchroscope	Sixth engine change	
Starter relays	1,500	2,000
Argon-light dynamotor	1,500	2,000
Wing-deicer valve motors	1,500	2,000
Flasher mechanism, navigation lights	3,500	3,750
Pitot heater	4,500	5,000
Master switches, battery	5,000	6,000
Primer solenoids, booster coils	5,000	6,000
Fuel-flowmeter power supply	5,000	6,000
Landing-light relays	6,500	8,000
Electric system, including conduit, wiring, junction boxes, lights, switches, and so forth	(Airplane overhaul) 6,500	8,000
Batteries	Life of battery	

* Engine-change period—normal—750 hours; maximum—775 hours.

very promising. An additional advantage accruing from the use of polythene in place of rubber is the possibility of eliminating the tinning of the copper cores.

Conclusion

From the foregoing it will be seen that developments are proceeding actively in Britain to provide electric-wiring installations for aircraft which will be reasonably efficient in operation and capable of being produced by wartime labor and production methods. The experience of four years' war operations in a variety of locations has been studied and employed as the basis for this development. Among these experiences is that gained by British personnel in operating and maintaining American types of aircraft in which single-pole electric installations are employed. Although until recently all British aircraft employed an insulated two-pole system, new types of aircraft are being engineered on the single-pole system employing the air frame as the negative return. The adoption of this practice will be advantageous from the points of view of weight saving and the ability to use electrical equipment which has been designed for this system of supply.

niles of flight. Every airplane is subected to a total of four types of inspection nd one period of overhaul. The most requent inspection is the service inspecion which is conducted at every regular chedule stop and which consists of a isual check of the airplane's control urfaces and propeller blades, and a check f the oil and gasoline in the tanks. At hat time any pilots' complaints are acted pon where necessary to render the aircraft airworthy. This inspection will ccur on the average of every two hours

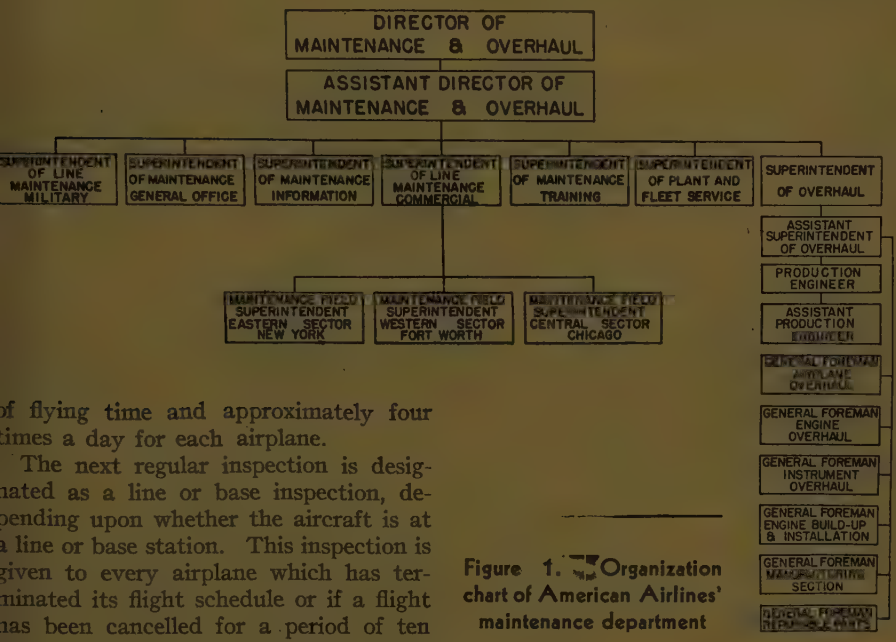


Figure 1. Organization chart of American Airlines' maintenance department

of flying time and approximately four times a day for each airplane.

The next regular inspection is designated as a line or base inspection, depending upon whether the aircraft is at a line or base station. This inspection is given to every airplane which has terminated its flight schedule or if a flight has been cancelled for a period of ten hours or more. Two men spend approximately five hours in making a comprehensive visual inspection of the aircraft's exterior and of its interior accessory equipment. At this time a functional check of the electric system is made, and the airplane's storage batteries are replaced with freshly charged batteries. All units are checked for their replacement time and are changed if necessary. Each aircraft receives a line or a base inspection at least once every 24 hours (Figure 2). A base inspection is essentially a duplication of the line inspection with the addition of a few more complete functional checks made possible by the use of test equipment located at the base stations. Approximately 75 items of equipment are reviewed at the time of a base inspection.

A major inspection is given each aircraft when 200 hours of flight time have been accumulated with a maximum limit of 230 hours since the last major inspection. A major inspection is performed at a base station and requires approximately 100 man-hours. It is a repetition of the aforementioned base inspection with the addition of a functional check of the hydraulic and landing-gear system with the ship on jacks. A more comprehensive engine inspection is made and consists of an oil change and a check of the valve clearance and of cylinder compression. The calibration of the exhaust-gas analyzer (fuel–air–ratio indicator) is checked by passing a test gas of known ratio through the analysis cell (Figure 3). The major inspection covers some 100 items of the aircraft.

In addition to the various airplane inspection periods the maintenance procedure requires that every component of equipment be overhauled periodically. The time between overhaul for any accessory is determined primarily by the past service experience with that equipment. Furthermore, wherever possible this time is adjusted to coincide with one of the inspection or engine-change periods. As it turns out most of the periodic replacement times are equal to or multiples of the engine-change period. This period at the present time is 750 to 775 hours. Those electric accessories which form a part of the engine-mount assembly are expected to give reliable service for at least the engine-change period, thereby facilitating the handling of such parts during their replacement. These units include the aircraft engine-driven generators, starter motors, magnetos, booster coils, resistance-type temperature bulbs, and tachometer generators. The engine spark plugs are the outstanding exception to this program, requiring replacement every 60 hours.

Other electric equipment not necessarily a part of the engine assembly also is removed during engine-change periods and at various multiples thereof. Table I indicates the various times at which the different items of electric equipment are removed for overhaul.

It is interesting to note the increase in engine-change periods during the past six years as this is a factor which directly affects the periodic replacement time for the majority of electric equipment (Figure 4). That is, if the overhaul time of various accessories could not be advanced with the engine-change time, the entire maintenance program would be seriously inconvenienced. There has been some discussion in regard to raising the engine-change time to 1,000 hours, and it is not unreasonable to expect such a change in the next few years. In view



Figure 2. Battery service at line inspection



Figure 3. Exhaust-gas-analyzer-cell installation

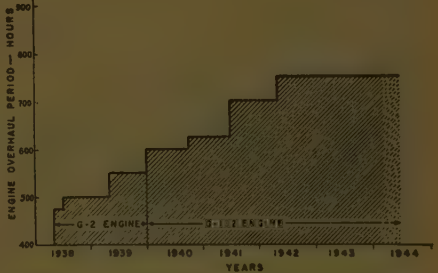


Figure 4. Trend of engine-overhaul period Wright G-2 and G-102 engines



Figure 5. Fuel-flowmeter calibration



Figure 6. Engine-tachometer calibration



Figure 7. Electric-instrument overhaul

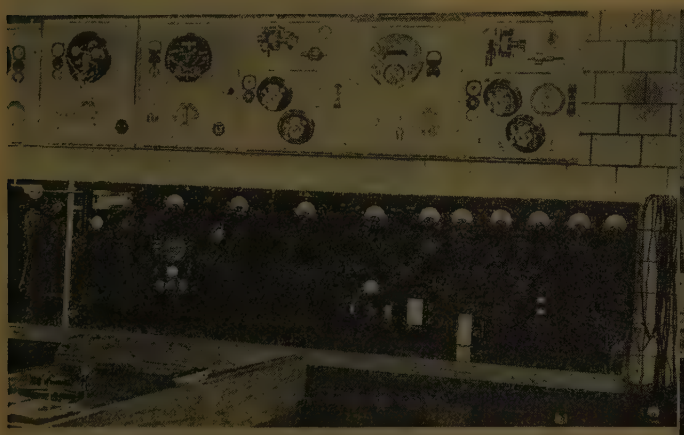


Figure 8. Electric-instrument calibration bench



Figure 9 (right). Bench test of electric-motor-driven accessories

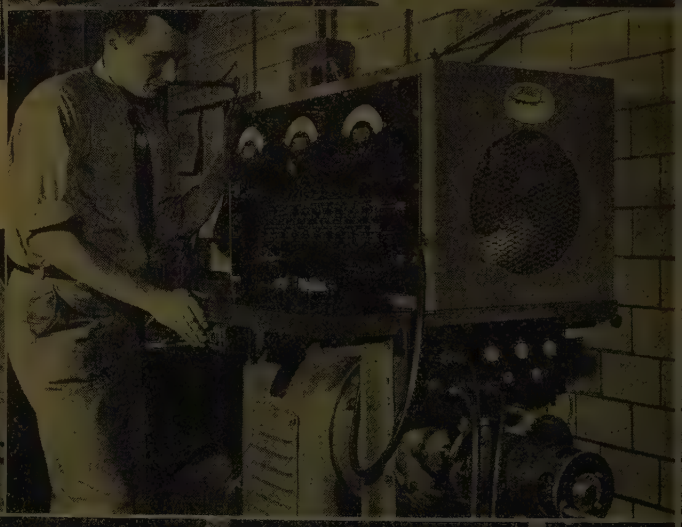


Figure 10 (lower right). Generator and control-box test stand





Figure 11 (above left). Starter-motor test stand



Figure 12 (left). Magneto test stand

Figure 13. Spark-plug overhaul



Figure 14. Electric-instrument-panel removal at overhaul



Figure 15 (top). Center section, electrical junction box

Figure 16. Pilot's electric switch panel

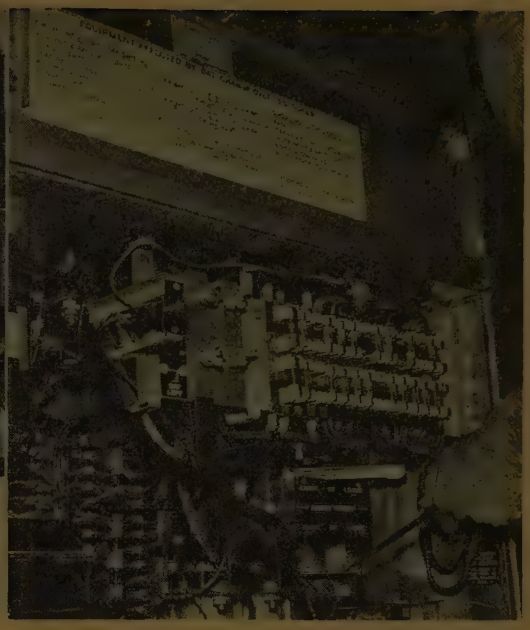


Figure 17. Main electric junction box

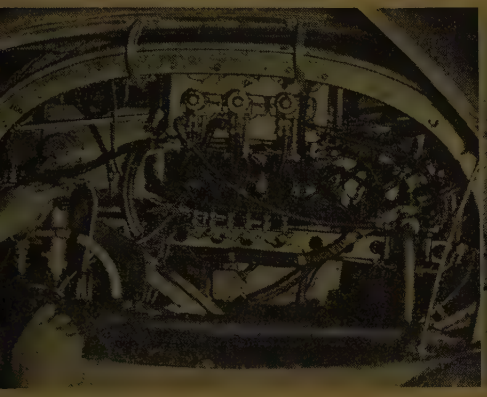


Figure 18. Rear view of instrument panel of Douglas DC-3 airplane



Figure 19. Individual-seat-light installation



Figure 20. Fabricating electric conduit for replacement



Figure 21. Landing-light installation of the leading edge of the wing



Figure 22 (above right). Engine electric-harness overhaul



Figure 23 (right). Engine ignition-harness overhaul



Figure 24. Space available for servicing engine electric accessories

of this trend in engine-change period, it is somewhat disappointing to note the newly created Army–Navy and other specifications setting the continuously operating time requirement for accessory equipment at 400 hours. It sincerely is hoped that the aircraft electric-equipment manufacturers will design for the 1,000-hour mark so that their equipment will fit in with the expanding airline requirements. In this respect the manufacturer should consider that the airline operator must balance maintenance cost against the operating cost and that, in some instances, the heavier but less troublesome article is the less expensive in the long run.

The actual overhaul procedure to which each unit is subjected is determined largely by the service experience with the unit. However, in many cases it corresponds 100 per cent with the manufacturer's service instructions. Figures 5 through 13 illustrate typical overhaul procedure on a number of electric items.

After approximately 6,500 hours and not more than 8,000 hours of operation the airplanes are brought in to New York for a ship overhaul. In addition to all the structural inspection and repair the remainder of the electric system, namely, conduit, wiring, junction boxes, switches, and so forth, is inspected and replacements are made where necessary (Figures 14 through 23). In general the aircraft conduit and wiring in the engine nacelle compartment must be changed at this time. The fuselage conduit and wiring, however, has been found in satisfactory condition after 18,000 hours (approximately [illegible] years) wire in the engine section, commonly referred to as the engine harness, must be replaced at each engine-overhaul period.

Up to this point we have discussed the ideal maintenance procedure in regard to inspection and replacement of equipment for overhaul. Supplementing this problem is another in which provision has to be made for replacement of units which fail in service. Briefly the problem is one of stocking a sufficient number of spare units at strategic stations, the number of units and stations being determined from past experience, so that flight schedules will suffer a minimum of interruption. In addition some means must be provided for returning the used part to New York where it will be overhauled and returned to service. In order to expedite the movement of these parts, it is necessary to ship 60 per cent of them by airplane to New York. It might appear on the surface that the airlines are saving themselves a transportation bill; however, such reasoning is fallacious inasmuch as the present operation at high-load factors places a penalty on any additional weight which does not result in an increase in revenue. American Airlines estimates the addition of one pound of such weight to cost approximately $88 per airplane per year.

It is because of the necessity for replacements of units in the field that the airlines have placed so much emphasis upon design of equipment and installations for ease of maintenance. It is important to note that design for easy servicing requires the co-ordination of both the equipment and the aircraft manufacturer. That is, an aircraft accessory can be designed with the most effective quick disconnect features, but, if it is so installed on the aircraft that it requires a removal of stress plates, miscellaneous lines, and other items of equipment before it can be reached, it is obvious that the maintenance problem has not been satisfactorily solved (Figure 24). The

Figure 25. American Airlines' daily operations chart

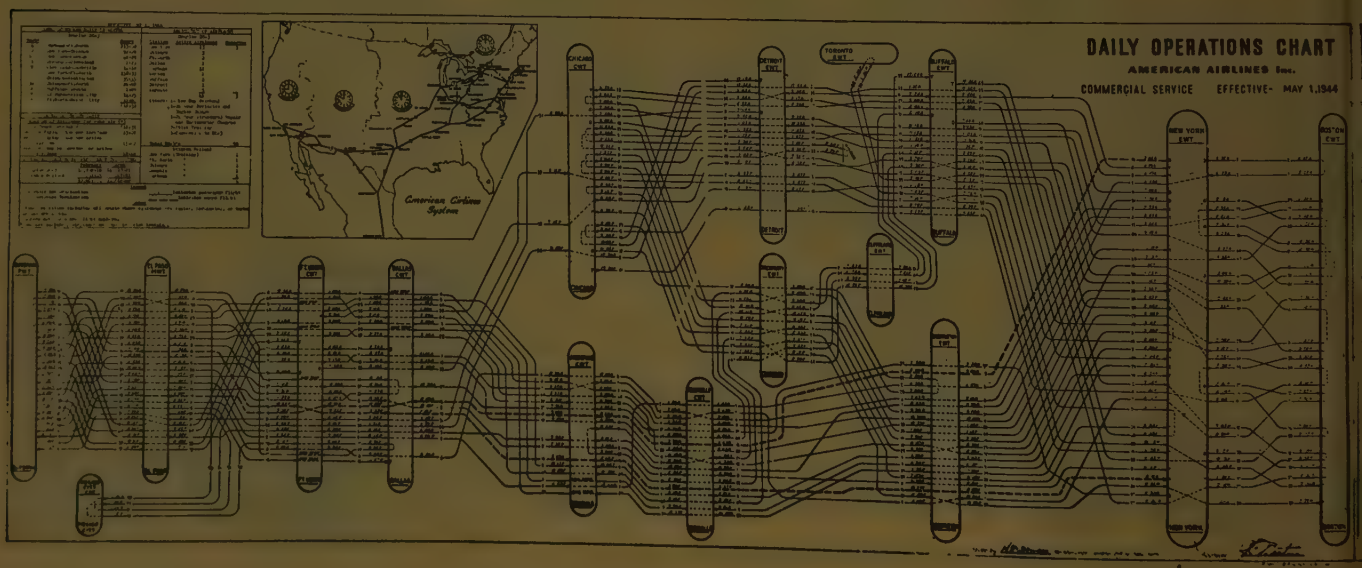




Figure 26. Airplane routing office

equipment manufacturer owes it to himself to follow up on the installation of his equipment in order to insure that the installation of his product is not unfavorable to its ready servicing. This is particularly important when the unit of equipment is of such nature that it requires frequent servicing. We might cite as an example the spark plug for which an installation was designed requiring the removal of considerable cowling. Removing one spark plug necessitated approximately nine man-hours of labor. As a matter of fact it is this type of design which is giving impetus to the creation of readily removable engine-mount assemblies, since it appears that it would be quicker to change the entire engine than it would be to change the spark plugs.

The entire maintenance system would be impossible without a smoothly running record and report system. The goal of this system is to record all changes and inspections made to the airplane and its parts so that it is possible to determine at any time when a part was removed or installed on an airplane; its identifying serial number; the mechanic who replaced the part; and the complete overhaul history of the unit, including the trouble found, correction made, and the mechanic who did the work. Some idea of the scope of this system can be gathered from the fact that there are some 65 parts in each airplane of which overhaul records are kept.

Equally important is the job of the maintenance dispatch office which has the responsibility of controlling the scheduling of airplanes so that they will be at their proper stations within the designated limits of time for inspection periods, engine-change periods, and overhaul periods. Some idea of the scope of this job can be obtained from Figure 25 which illustrates the various scheduled routes over the system. Each base station furnishes a daily report by radiogram listing the scheduled routing of all airplanes in its individual control sector. This information includes data on engine time since overhaul and time since the last inspection of the airplane. From this mass of data the maintenance dispatcher issues routing instructions which are designed to bring the airplane to an appointed station in time for this required servicing (Figure 26). In spite of the complexity of the system, the average engine-change time of 42 engines for the month of April 1944 was exactly 750 hours.

This discussion would not be complete without a word about the activities of the airline's engineering department and a brief description of its functions. In a broad sense the engineering department acts as a technical consultant to the maintenance department with the object of improving the service life of the equipment and thereby increasing the efficiency of the operation. A service-report system is utilized to keep both the maintenance and engineering departments informed of the current difficulties. These reports are initiated in the field at the time the trouble is experienced and include all pertinent information which will be of value in the determination of suitable corrective action.

Not only is the existing equipment under constant investigation for possible improvements, but also a weather eye is kept on the industry at large to utilize the fruits of improved design in order to obsolete less satisfactory equipment. It is the engineering department's responsibility to maintain contact with all parties concerned in the design and manufacture of the aircraft and to negotiate desirable improvements in any of the associated equipment. The organization of American Airlines' engineering department is shown in Figure 27 and illustrates the degree of specialization which is necessary in order to adequately cover the broad field of engineering which makes up the modern air-transport system.

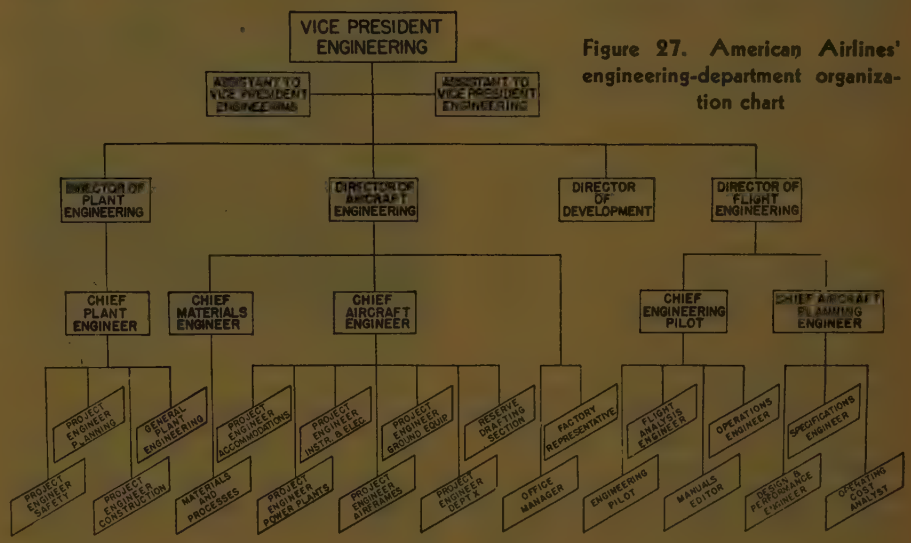


Figure 27. American Airlines' engineering-department organization chart

It is hoped that the foregoing discussion will have served its purpose of bringing home to the aircraft electric-equipment designer a picture of the end use of his product. More important, however, is the designer's recognition of the fact that his equipment must fall into a predetermined pattern of inspection and overhaul procedures which are not necessarily set up to accommodate his particular equipment but rather to provide an economical method of insuring the airworthiness of the complete airplane.

Electric Connections on Aircraft

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AN electric system is as good as its electric connections. Poor electric connections on aircraft easily can cancel the tremendous advantages obtained by the use of electric systems. Little attention is given to this unglamorous but vital subject because of the more absorbing and interesting details of the electric system as a whole. Only after an unexpected and disconcerting electric-connection failure in service does the engineer realize its importance. Such failures on domestic appliances are not generally serious but on aircraft may involve many lives, much valuable equipment, and, in fact, the success of our war effort.

This paper describes factors and design details which must be considered in the design of successful electric connections on aircraft.

Types of Electric Connections

Electric connections may be listed according to their service as follows:

1. Permanent type.
(*a*). Soft soldered.
(*b*). Hard or silver soldered.
(*c*). Welded.
(*d*). Crimped.

2. Semipermanent type.
(*a*). Screw and terminal.
(*b*). Stud and terminal.
(*c*). Special.

3. Quick disconnect type.
(*a*). *AN* connector.
(*b*). Other special types.

4. Sliding connections.

5. Operating connections.

The effects of different kinds of metals, alloys, or electrolytes in the circuit usually can be neglected except for thermocouple and other similar low-voltage circuits. These exceptions require connections to be made with like materials, or they may require a design in which paired junctions are always at the same temperature. For example, a soldered joint making a connection between two copper wires will cause little difficulty when the two junctions are close together, except in the most critical circuits. Clamped or welded connections between like materials are required where undesired thermal electric voltages are to be avoided.

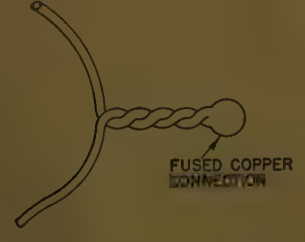


Figure 1. Flame-welded connection between two enameled wires with a twisted portion for mechanical support

Permanent Types

Soft Soldered. In spite of the fact that the art of soft soldering electric connections is relatively old in the electrical industry, it still must receive constant attention in production lines beginning at the point where the insulation is stripped from the wire.

The preliminary insulation stripping operation preferably should be accomplished without nicking, swaging or scraping any strands in the wire. The insulation on aircraft wire per *AN-J-C-48* should be cut evenly, leaving the wire clean and free from insulation. For high-quantity production, automatic machines have been devised for cutting the wire to length and removing the insulation from the ends. Because of variations in wire size and stranding and variation in adjustment of the cutting knives, it is practically impossible to prevent occasional nicked or scraped strands. The following rules have been set up in some production lines to aid in inspecting stripped wires:



Figure 2. Screw terminal block

1. Not more than ten per cent of the number of strands can be nicked.

2. Strands which are scraped or swaged not more than 25 per cent of the diameter are acceptable.

3. If it is assumed that the nicked strands may break during assembly, the following maximum number of broken strands after assembly are allowable:

Strands in Wire	Maximum Allowable Broken Strands
7	1
10	1
16	1
19	2
26	2

Hand-operated wire insulation strippers may be used for laboratory purposes, service work, and so forth, but are subject to the same dangers of nicking and scraping.

The design of the wire insulating layers can have appreciable effect on the ease of stripping. The first or separator layer can be made of material such as number 400 electrical tape which will rupture readily and allow the other insulating layers to slide off the wires easily when they are cut. The stripping knives then can be set to the outside diameter of the wire plus the separator layer which reduces the possibility of damaging the wire.

Insulation also may be removed by burning with electrically heated blades at the desired point, allowing the insulation to cool, and pulling the insulation off the end of the wire. The interval for cooling is necessary so that hot and softened insulation, such as Flamenol, will not be forced between the strands.

Enamel can be removed safely from single fine wires, 0.004 inch or smaller by rubbing with fine emery or crocus cloth. Chemical means may be used to remove the enamel, provided the chemical is subsequently thoroughly washed off. The enamel on stranded enameled wire may be removed by heating the wire and dipping the hot portion into denatured alcohol.

After the insulation has been removed from the end of aircraft wire (per *AN-J-C-48*), it is preferable to twist the strands tightly together in a production twisting machine. They are then dipped into a rosin and alcohol soldering flux and into a solder pot. This extra "tinning" operation saves much time in a production line because the strands of the wire stay in place while the mechanical connection is being made.

When making connections between wires and vacuum-tube socket or similar terminals, the wire should be looped through eyelets or slots and wrapped around to give a mechanical support before soldering to the terminal.

The soldering operation is completed by applying flux, heat, and solder to the wire and terminal so that the solder wets the metal surfaces. The joint must cool so that the solder freezes solid before any strain is applied to the joint. Any relative movement between the wire and terminal during the freezing renders the connection mechanically and electrically weak.

Rosin or rosin in alcohol is the only flux which has proved satisfactory for aircraft electric connections. The minimum amount of flux should be used. Rosin-cored wire solder having rosin in

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Figure 3. British wire terminal having combination screw, crimped, and clamped features

five per cent or less of the cross-sectional area can be used conveniently. When soldering near electric contacts, care must be used to prevent the rosin from splattering or running onto the contacts. Excess rosin may flake off onto contact surfaces after it has dried out, which is an important reason for using the minimum flux. The soldered connection may be cleaned with alcohol to remove all excess rosin, or it may be coated with a colored lacquer or Glyptal paint to prevent corrosion and flaking of the rosin.

Excess solder on a joint is not only wasteful, but also may cover a poor joint. A beautifully rounded ball of solder on a joint is to be avoided. The form of the strands of wire should show on a well-soldered joint, with fillets of solder between strands and terminal indicating a thorough wetting by the solder.

Hard or Silver Soldered. This type of connection is normally used only on circuits which are subject to temperature higher than the softening point of the soft solders. It is very important that the fluxes be thoroughly removed to prevent corrosion in service.

Welded. Both spot and flame welding are used extensively for special connections. Spot welding requires a very high grade of control to insure satisfactory joints because it is difficult to determine the quality of the joint by visual or nondestructive means.



Figure 4. British terminal block using wire terminals having combination screw, crimped, and clamped features

One successful production method of connecting copper wires larger than 0.010 inch consists in twisting the wires together for mechanical support and melting the ends of the wire down to a ball with a hot flame as shown in Figure 1.

With Formex or enamel insulated wire, it is not necessary to remove the insulation when using this method.

Crimped. Several types of crimped connections have been developed for use between wires and terminals. These have the advantages of eliminating flux and solder troubles and speeding up production. Some types are crimped on both the wire strands and insulation to form a very good mechanical support. Standard crimped or solderless types for aircraft use are specified under Army–Navy Aeronautical Standard *AN*-659. One new type recently put on the market has an insulating coating on the crimped portion which eliminates the necessity of covering the terminal shank with an insulating sleeve.

The quality of the connection obtained with solderless terminals is difficult to determine by visual inspection. The size of the terminals and crimping tools must be held to close tolerances which have been proved to produce satisfactory joints by destructive tests. The crimping pressures and the physical characteristics of the crimped tube must be carefully controlled. One type of crimping tool cannot be used with all makes of terminals because of differences in size and design. The National Aircraft Standards Committee and Army and Navy are taking steps to obtain standardized types and tools, but at the present time best results are obtained by using the tools and methods according to the terminal manufacturers' recommendations.

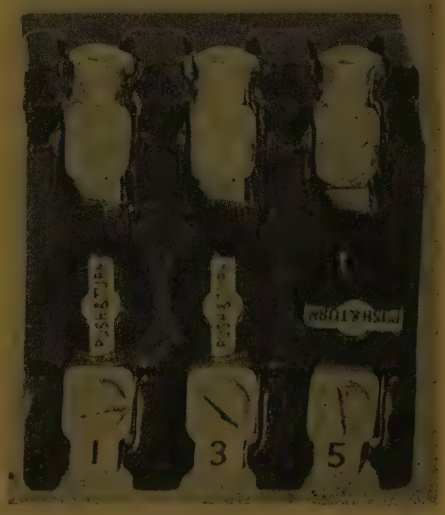


Figure 5. British terminal block for quickly disconnecting circuits

Semipermanent Types

Screw and Terminal. Screw terminals are used where space is restricted, and the currents are limited to about 30 amperes. Figure 2 shows one style which is used extensively. The number of connections which can be made under the head of a screw is limited by the length of the screw. Generally two connections are the maximum. It is also advisable to use a split spring lockwasher between the screwhead and connecting terminal.

A combination screw, crimped and clamped terminal connection used on British aircraft, is shown in Figures 3 and 4. This design has several obvious advantages in assembly and servicing. The wire terminal is a cylindrical design which is crimped on the wire and insulation. This is held by a simple screw-clamp arrangement which can be removed from the terminal board as shown.

Another British terminal block design for disconnecting circuits quickly is shown in Figure 5. Circuit isolation for testing, repair, and so forth, may be accomplished quickly by pushing the button in the terminal block and twisting 90 degrees.

Stud and Terminal. Stud-type terminals are the most reliable of all types. They will carry the highest currents and are the least subject to vibration. This is borne out by experience on aircraft generators on which stud terminals have replaced *AN* connectors.

The National Aircraft Standards Committee has issued standards on stud connectors mounted in special panels, ranging from number 6 screw studs to three-eighths-inch-diameter studs. These are described in National Aircraft Standards 17 through 22, inclusive.

A new type of stud terminal which clamps directly on the wire strands recently has appeared on the market and is shown in Figure 6. This eliminates the necessity of applying a terminal to the wire when making a connection, but the wire would be damaged if frequent removals from the stud were required.

Special. Ground connections or frame connections which are part of an electric circuit, and radio shielded con-



Figure 6. New type of stud terminal which clamps directly on the wire



Figure 7. n- and socket-type connector with "banana" pin construction

nections may be classified as special semipermanent types. In addition, many moving metallic parts on aircraft must be electrically connected together with low-resistance connections in order to reduce electrical disturbances which cause noise in the radio receiver. Special bonding techniques have been developed for making these connections. A flexible stranded bare cable with crimped or soldered terminals is generally employed. The connection between the terminals and the stationary and moving parts presents problems due to the different metals and surface finishes. A stable connection with not more than 0.0005 ohms resistance is generally required. At the same time, the parts must not be subject to galvanic or normal corrosion due to climatic conditions. Protective finishes on aluminum and magnesium alloys must be removed in order to obtain the low-resistance connection, and the completed connection must be protected against corrosion. Pure aluminum washers are used between metal combinations subject to galvanic action, such as magnesium and copper. Painting the entire connection with zinc-chromate primer or similar material provides reasonable protection.

Tinned bronze lockwashers with external teeth (*AN*-936) can be used between the terminal and part to be bonded to provide a low-resistance connection by the multiple point contacts which are thereby obtained. Some practices rely on these washers to cut through insulating surfaces, such as the coating on anodized aluminum alloys, without any other preparatory cleaning of the surface.

Electric motors, generators, and other electric devices may use the frame to carry one of the circuits to a supporting part. Both mounting surfaces must be cleaned thoroughly in order to make a low-resistance metallic contact. The edges of the mounting surfaces must be protected at assembly by zinc-chromate primer or equivalent to prevent corrosion from attaching at these unprotected points.

The radio shielding of sources emitting strong radio interference, such as an aircraft-engine ignition system, presents a connection problem which requires accurate mechanical positioning of the parts in contact and joints that are watertight. Plain flat surfaces at the joints provide openings between the bolts a few tenths of a mil wide through which radio noise leaks. A continuous electric contact completely around the joint has been found necessary to keep radio noise contained within the ignition system. This can be accomplished by cutting a square groove in one joint surface and inserting in that groove a round resilient braided wire-covered gasket. When the joint is assembled the braided gasket is forced to contact the flat surfaces by the resilient material inside the braid. A second method for obtaining continuous electric contact around a joint makes use of a flat gasket consisting of wire screen impregnated in Neoprene. This gasket has been found to be less effective than the round braided gasket and leaks radio noise under certain conditions.



Figure 9. Socket used on Japanese connector



Figure 10. Disassembled German 20-circuit connector



Figure 8. Typical sockets used on *AN* connectors

Quick-Disconnect-Type Connectors

When electric apparatus must be quickly and frequently disconnected, special quick disconnect types are used. Some degree of reliability is usually sacrificed to obtain the quick disconnect feature. Numerous and varied designs have been made since the beginning of the electrical industry. In an effort to standardize, the joint Army–Navy specification *AN*-9534 was issued. To supplement this specification and obtain improved designs, specification *AN-W-C*-591 was issued and is under revision at the present moment. Before discussing the merits of the *AN* connectors it might be well to consider basic designs.

Simple Direct-Pressure Type. This type consists fundamentally of two metallic parts which are held together by direct pressure to make the circuit. Generally three points of contact between the metallic surfaces carry most of the current. The contact surfaces easily may be inspected and cleaned. Dirt may come between the surfaces and prevent electric contact.

Pin and Socket Type. This type consists of a pin which slides into a socket and incorporates means for maintaining radial pressure between the pin and socket. The pin can be inspected and cleaned without difficulty. The socket surfaces are difficult to inspect, but they may be cleaned by special means.

In a few designs the spring pressure is obtained in the design of the pin, as for example, the "banana" pin construction shown in Figure 7. A split pin construction also has been used but is weak mechanically.

The most common types of pins and socket connections use solid pins and a socket which, by its design, applies spring

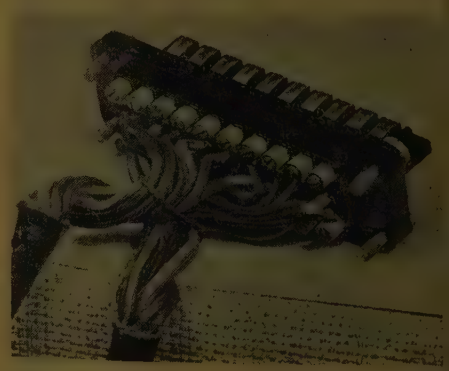


Figure 11. Rear view of German 20-circuit connector plug showing connection terminals

pressure to the pin. A wide variety of socket designs have been developed, a number of which are shown in Figure 8. A Japanese socket design is shown in Figure 9. This was a split-screw machined socket with the addition of a helical spring at the end of the socket and a *C*-shaped cylindrical spring on the body. Our experience in the operating field has shown that the use of these designs should be limited to circuits carrying less than 60 amperes. A discussion of *AN* connector design will be given later in this paper.

Sliding Contact Type. A connector design found on recent German aircraft is shown in Figures 10, 11, 12, 13, and 14. A pin is molded in the surface of the receptacle which makes a sliding contact with a flat type of spring contact on a molded plug. This design is one of the few in which the connecting surfaces easily can be inspected and cleaned. The receptable plugs are held together by a simple toggle spring arrangement, as shown, and a synthetic rubber gasket provides a tight seal to prevent water from entering.

AN Connectors

At the time this paper was written, *AN* connectors were described in specifications *AN*-9534*a* and *AN-W-C*-591. It is assumed that the reader is familiar with these specifications. Our experience in using *AN* connectors made by a wide variety of manufacturers, according to these specifications, in all types and sizes, indicates that the specifications are woefully inadequate for the insurance of obtaining satisfactory *AN* connector performance. The successful operation of *AN* connectors in the combat field has been due mainly to the choice of available connector designs and extra inspection before assembling in equipment.

One of the weakest parts of *AN* connector design from practical considerations has been in the socket. A satisfactory socket design must be one which cannot be damaged by inserting the pin at any angle which may be obtained during the process of mating the parts. It should not be damaged by test pins such as are used on circuit testing instruments.



Figure 12. Plug-side view of German 20-circuit connector

Several successful designs use a solid ring at the entrance end of the socket to guide the pin into place (see Figure 8). An oversize test pin cannot be inserted in such a socket. Other successful types use auxiliary springs and bands to maintain positive contact. The sockets may be inserted in a molded phenolic insulator which limits the possible motion of the sprung sections of the socket so that they do not go beyond the elastic limit.

Adequate locking means to hold the plug and socket together are needed. Safety wire through a hole in the coupling nut to a mounting screw is very effective.

Complete 100 per cent inspection of *AN* connectors is imperative. Inspection details to be noted may be listed as follows.

Connector Shells

1. The shells shall have a good appearance with no porosity, blow holes, burrs or cracks. All sharp corners shall be broken with a $^1/_{64}$-inch radius unless otherwise specified.

2. The shell shall be of sufficient thickness so that the connector will be plugged to its mating unit in the correct position only.



Figure 13. Socket of German 20-circuit connector

3. The shell tongue shall have enough thickness and strength so that it will not break in use and will allow the connector to be plugged to its mating unit in the correct position only.

4. The shell, when spun over the phenolic insert, shall be so spun that stresses are not set up in the insert which cause the insert to break or shear.

5. Information stamped on the shell shall be correct and easily legible.

6. The snap-ring slot in the shell or in the coupling nut shall have square sides with sufficient width and depth so that the snap ring cannot pull, slide, or twist out of the slot during use of the connector. A special slot which wedges the snap ring against the coupling nut when the nut is tightened has been found to be very effective with some designs.

7. The shell threads shall be centered properly on the shell so that they are not too deep on one side and too shallow on the opposite side. The threads shall not be broken nor contain broken-out sections, and the threads on half of the shell shall be in complete alignment with those on the other half. Burrs on the threads at the casting parting line shall be removed.

8. The threads on all connectors shall be bare, except for antiseize compound, and shall have no varnish, shellac, paint, or anodic treatment.

9. The antiseize compound on the threads shall not contain grit and shall conform to *AN* specifications.



Figure 14. Assembled German 20-circuit connector

10. Flanges on the inner parts of the shell shall be square and shall project sufficiently to prevent an inner shell or phenolic insert from pushing or pulling out of the shell.

11. The connector having split shells shall be so held together by sufficient supports, and so forth, that the inner shell or phenolic insert cannot be forced out of the connector.

12. The coupling nut shall be so fastened to the shell that it can be tightened thoroughly with pliers without pulling from the shell.

13. Springs on split-shell connectors shall be of sufficient strength so they do not break nor slide out of position.

Phenolic Inserts

1. The phenolic material shall not be brittle. The pins and sockets shall not push out of the phenolic backing disk nor pull through the front of the phenolic insert during plugging and unplugging of the connector. The phenolic guide tongues, slots, edges, phenolic material between contacts, letters, shoulders, flanges, and ridges shall not fracture during connector use.

2. The phenolic material shall be an insulating material and must not contain metal dust, metal chips, or other conducting material that will allow short circuits between contacts or between contacts and shell.

3. The phenolic insert shall be so designed and built that all contacts are located properly according to *AN* drawings, and all pins of the same size must protrude the same amount inside the shell.

4. The phenolic backing disk shall have a slot fitting to a tongue in the shell, or vice versa, which will make it possible for the backing disk to be assembled in the correct position only.

5. The manufacturer of the connector shall assemble the phenolic insert of the socket-type connector with the backing disk of the socket-type connector, not with the backing disk of the pin-type connector, and vice versa. When the backing disks are incor-

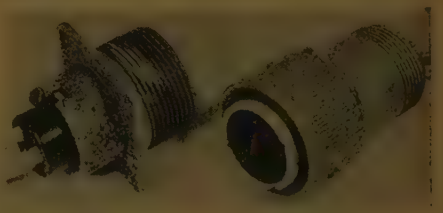


Figure 15. *AN* connector after 96-hour humidity test



Figure 16. Sample Monowatt Electric Corporation *AN* snap-in-type connector

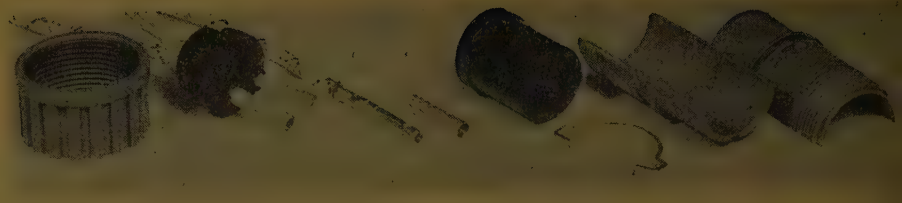


Figure 17. Sample Monowatt Electric Corporation *AN*-3106-18-5*S* snap-in-type connector disassembled

rectly assembled, the connector lettering is reversed.

6. The tongue on the phenolic insert shall be large and strong enough so it cannot shear or break off. This difficulty is still being experienced on size ten connectors without supporting shells on phenolic inserts.

7. The holes for the metal sockets in the phenolic inserts shall be of such size and shape that the metal socket can readily accommodate itself to the pin without the pin sliding down the outside of the socket.

8. With the connectors of the molded in pin design, the phenolic material must not shear from the pin shoulders and allow the pins to push through the sockets, for all pressures on the pins up to ten pounds.

9. The lettering at the front and rear of the pin insert must coincide with that at the front and rear of the socket insert.

10. It has been found desirable to have all the solder cups face in one direction.

11. The phenolic insert is not to push nor pull out of the connector shell during plugging or unplugging of the connector with its mating part.

Contacts

1. The pins must be of a strong durable material that will not bend nor break because of possible forcing or twisting when the connector is plugged or unplugged to its mating part.

2. The pins shall have a durable plating of sufficient thickness to prevent the formation of corrosion.

3. The pin shoulder diameter shall be large enough so that the pin will not push through the phenolic backing disk when the connector is plugged or unplugged to its mating unit.

4. The solder cup shall not break off the contact during normal use of the connector.

5. No pin is to push out through the backing disk with a pressure of ten pounds applied to it. (At General Electric Company's request the American Phenolic Corporation was set up to apply a ten-pound pressure test to each pin in the connector and mark each connector so tested with a green dot. This test increases the connector price, but the improved quality has been justified.)

6. The socket design shall be such that the sockets will not be damaged by any possible twisting or forcing when the connector is plugged to its mating unit or when test pins are inserted into the sockets. Sizes 16 and 20 sockets made by the American Phenolic Corporation, and sizes 12, 16, and 20 sockets made by the Monowatt Electric Corporation have reinforcing sleeves made of durable bronze not less than eight mils thick. (At General Electric Company's request, on and after October 23, 1943, Amphenol and Monowatt supply General Electric only with connectors that have been given an extra 100 per cent inspection at the manufacturer's plant to make certain that all small-size sockets, as mentioned previously, have reinforcing sleeves. The connector manufacturer shows this extra 100 per cent inspection by a red dot on the connector. This inspection should be applied to all banded-type sockets made by all manufacturers.)

7. The socket should be so constructed that liquid solder in the solder cup cannot leak through to the front of the socket and prevent insertion of the pin.

8. The hole in the socket should be of such shape that the pins readily slide into the sockets the first time.

9. The insert socket holes should be of such size that the sockets are free to adjust themselves to the pins when the connectors are plugged. It is preferred to have the pins fixed and the sockets floating.

When *AN* specifications are revised, it is hoped that some identifying means will be devised so that connectors according to the revised specifications can be distinguished from older types.

Corrosion protection of the *AN* connectors is not required in existing specifications. Figure 15 shows the corroded condition of an *AN* connector shell after exposure to 100 per cent humidity for 96 hours at 33 degrees centigrade. The threaded portions which were covered by antiseize compound are in perfect condition. In tropical and salt-water service, connector shells, pins, and sockets must be coated with a protective grease, for example, lanolin, to insure continued operation. This results in a very messy condition, as dust and dirt is picked up on the grease.

A new *AN* connector design with provision for safety wiring the hold nut and other advantageous features is shown in Figures 16 and 17. Connections may be made between the pins and wire by crimping, soldering, or both. Holes in the crimped tube opposite the crimp permit inspection of the crimping. The design of the insulating parts gives greater creepage distances and mechanical support to the wires without the use of insulating sleeving.

Under desert conditions, dust in the coupling nuts on *AN* connectors causes the threads to jam. The German connector design shown in Figures 10 through 14 is not affected by dust and can be disconnected quickly under any conditions. It is hoped that this advantageous characteristic may be incorporated in a future *AN* design.

Sliding Connections

The design of sliding connections is an uncompleted art. The sliding connection may be on a resistor, a commutator, or a slip ring. The choice of materials and design is controlled by many factors and variables which are not thoroughly understood. With their use on aircraft, the additional effect of high altitudes must not be overlooked. Not so long ago we were astonished when our ordinarily good carbon brushes wore out on commutators in a matter of ten minutes or less. A great deal of research effort has been required to overcome this difficulty. Today designs can be made which will operate under such conditions. The details naturally cannot be discussed because of wartime restrictions.

It is safe to say, however, that each new application must be tested thoroughly under all operating conditions. The same solution to the sliding contact problem on one application does not necessarily mean that it will be good for a new slightly different application.

Operating Connections

Operating connections are normally used for control purposes. They make or break circuits carrying electric power at various voltages and direct or alternating load currents. They range from hand-operated switches to cam-operated magneto contacts.

Again it is very important to check operating connections with the exact loading conditions under which operation



Figure 18. Sample Monowatt Electric Corporation *AN*-3106-18-5*P* snap-in-type connector disassembled

Solenoid-Operated Control Valves

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THE presentation of a paper on electric aircraft apparatus by manufacturers of hydraulic control equipment is very likely to lead to a discussion[1,5] of the relative advantages and disadvantages of hydraulic and electric systems for accessory actuation. Let us evade this controversy for the present by stating that the writers are not advocating either an all-electric or all-hydraulic system but are describing a type of device that makes possible the utilization of some of the best features of both forms of power.

It commonly is conceded that one of the strong points of electric systems is the relative lightness, cheapness, and flexibility of electric wiring over hydraulic plumbing. This is particularly true when it is required to transmit only very small power. Consequently, a system that substitutes low-energy-level electric wiring for high-energy-level hydraulic plumbing would seem to offer advantages in cost and weight for some applications, especially in the larger more advanced airplanes. For example, it has been estimated[2] that for a multiengined airplane with a hydraulic pump on each engine the use of electric remotely operated valves for controlling wing flaps reduces the length and weight of plumbing to less than one half that required for an all-hydraulic installation with direct cockpit valve control.

An additional advantage offered by this electrohydraulic system is that it makes it possible to eliminate high-potential wiring and high-pressure plumbing from the cockpit and other critical areas, thus giving a safer and cleaner installation.

Obviously, the device that makes this electrohydraulic system practicable is the remotely controlled electrically operated hydraulic valve.

General Design

The general design requirements for these electromechanical control devices are about the same as for other similar aircraft components in that they must incorporate:

1. Utmost reliability.
2. Absolute minimum weight consistent with other requirements.
3. Operation over an extremely wide range of ambient temperatures and pressures.
4. Resistance to vibration.
5. Adaptation for use in the standard aircraft electric and hydraulic systems.
6. Auxiliary means of manual operation for use in case of electric power failure. This last requirement is not always necessary.

In designing one of these control devices the first step is to decide which of the several types of electromechanical drivers and hydraulic valves is to be used. The most commonly used drivers are electric motors, electrically heated vapor-pressure motors, and electromagnetic solenoids. Similarly, the usual valve types are the well-known slide, disk and poppet.

Various combinations of these drivers and valves have been proposed for specific purposes, but the writers believe that the combination that promises to fulfill most completely the requirements for the greatest number of applications is the electromagnetic solenoid driver applied to the piston poppet-type valve. This combination has the advantages of the simplicity and rapidity of operation (less than $1/20$ second) of the solenoid and the low fluid leakage and low operating power of the poppet-type valve. These characteristics are conducive to obtaining the lightest and most universal device.

In contrast, electric motors geared to valves as drivers are characterized by relative complexity, difficulty of obtaining manual operation, high weight for low-force valves but relatively low weight for high-force valves, and an intermediate operating time of from one to several seconds. Vapor-pressure devices are inherently simple and light but slow, requiring from approximately $1/2$ minute to several minutes to operate, and are likely to be more vulnerable than other devices. They are also difficult to accommodate to wide temperature and pressure ranges. Similarly, slide valves usually have greater fluid leakage than other types so that they are suitable only for hydraulic systems designed especially for their use. Disk-type valves, although simple and reliable, usually require relatively high rotary operating forces which make them suitable for adaptation to geared electric-motor drive but not solenoid actuation.

This discussion will be limited to the 24-volt d-c system, since that is the one in most general use at present.

Valve Design

Hydraulic valves are rated according to the volume of fluid that they will pass in unit time with a certain permitted pressure drop. This rated flow and the

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is required. Equivalent circuits often are misleading in action.

With the increased use of electric equipment on aircraft, relay contacts have been designed to carry currents in the order of thousands of d-c amperes at 24 to 30 volts. Operating contacts for 400-cycle a-c circuits at 208 volts are under development.

Conclusions

The vital importance of electric connections on aircraft is not generally appreciated by people accustomed only to the connections on home appliances. A poor connection on an electric toaster blows a quickly replaceable five-cent fuse, and the lady of the house has to endure the hardship of eating untoasted bread until the handy man makes a repair. One poor connection on an aircraft may effect the lives of many people and cause damage in the order of hundreds of thousands of dollars.

An engineer or production man may be proud to have failures due to poor electric connections on home appliances reduced to one per cent. But, if this percentage were allowed on each of the types of electric equipment on an airplane, would he be willing to fly in the plane?

For successful modern aircraft operation, electric connections must be engineered carefully, well constructed with the proper materials, 100 per cent inspected, properly applied, and properly serviced.

References

1. ARMY–NAVY SPECIFICATIONS. Connectors, electric aircraft; *AN-9534a*. Connectors, electric aircraft; *AN-W-C* 591. Bonding, electric (aircraft); *AN-B-10*. Terminal, electric solderless; *AN-659*. Washer, lock, external and internal teeth; *AN-936*. Installation of conduit, cables and bonding in aircraft; general specification for; *AN-W-14*.

2. NATIONAL AIRCRAFT STANDARDS. Terminal—electric, mechanical; *NAS-39*. Panel—connector, electric for number 6 posts; *NAS-17*. Panel—connector, electric for number 8 and number 10 posts; *NAS-18*. Panel—connector, electric for $1/4$-, $5/16$-, and $3/8$-inch posts; *NAS-19*. Screw post, connector panel, electric; *NAS-20*. Strip-insulating, nut, connector, panel, electric; *NAS-21*.

3. OK METHODS. American Phenolic Corporation.

4. AIRCRAFT ELECTRICAL ENGINEERING, Randolph Matson. McGraw-Hill Book Company, Inc., 1943.

5. BIBLIOGRAPHY AND ABSTRACTS ON ELECTRICAL CONTACTS. American Society for Testing Materials.

6. ELECTRICAL CONNECTORS FOR AIRCRAFT RADIO INSTRUMENTS, J. M. Caller. *Radio*, March 1944.

Paper 44-242, recommended by the AIEE committee on air transportation for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted April 21, 1944; made available for printing July 21, 1944.

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design of the valve are the important factors that determine the force and displacement necessary to operate the valve. Two general valve designs are diagrammed in Figures 1 and 2. Cutaway illustrations of practical embodiments of the same designs are given in Figures 3 and 4.

The valve of Figures 1 and 3 is unbalanced; that is, the fluid pressure is effective on the inlet face of the poppet so that the operating force is the sum of

1. The seal frictions.
2. The product of the fluid pressure and the effective poppet area.
3. The force of the return spring.

In the partly balanced valves of Figures 2 and 4 most of the force caused by the hydraulic pressure acting on the poppet face is neutralized by reducing the effective area. This has the tremendous advantage of reducing the valve operating force as is shown by Figure 6 but the considerable disadvantage of making the manufacture more difficult and costly.

The curves of Figure 6 give the operating forces required to crack and to hold open both balanced and unbalanced poppets. These curves apply to 0.266-inch diameter poppets for three-gallon-per-minute service. The values of operating force are only approximate, since they depend to a considerable extent upon

1. Detail design.
2. Manufacturing tolerances.
3. Types of seals.
4. In the case of the partly balanced valves, upon the degree of balance that can be obtained and still maintain the fluid leakage below the rated values set forth in government specifications.

The curves will serve, however, to show the considerable savings in driver work that is made possible by using the balanced-poppet principle.

When it is considered that approximately three-quarters of the weight of an unbalanced solenoid valve is driver weight, the added difficulty of manufacture involved in the partially balanced valve is entirely justified. Also, the trend is toward higher system pressures with correspondingly higher valve operating forces which enhances the value of balanced valves.

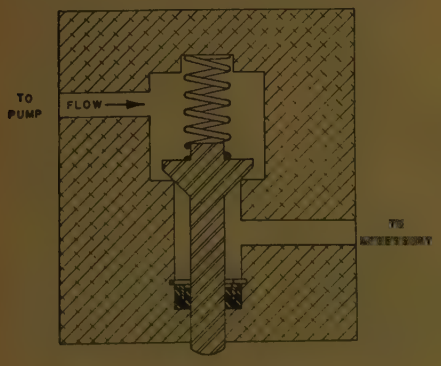


Figure 1. Diagram of unbalanced valve

This generalization in favor of partly balanced valves falls down for very small sizes. Figure 5 illustrates a cutaway model of a small shut-off valve originally intended for controlling the flow of anti-icing fluid. In this case the parts are too small and the operating pressure is too low to make a balanced construction practical. The savings of perhaps one ounce of weight out of five ounces would not be sufficient to justify the considerable added complication.

Solenoid Design

When the designer comes to the solenoid, he is in a position to take advantage of the low operating force and short travel distance that has been designed into the valve.

It would be out of place to include a detailed account of solenoid design in this paper, since this subject has been covered comprehensively in many previous publications.[3,4] Obviously, the design of solenoids for aircraft will follow the same principles incorporated in other electromagnetic devices with the exception that since weight is so very important even greater efforts than usual must be made to utilize all materials to the best advantage.

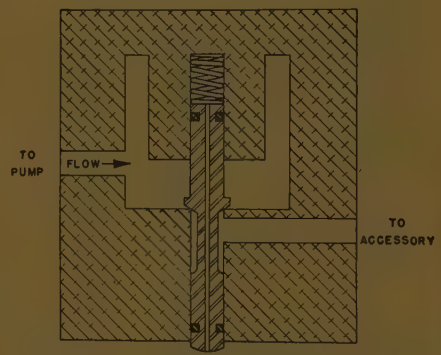


Figure 2. Diagram of balanced valve

Some of the major airlines have published statements giving a dollar cost to the weight of air-borne equipment in revenue lost in diverted pay load during the life of a commercial passenger or freight airplane. This figure averages several hundred dollars per pound for such airplanes. A similar figure for military aircraft would be impossible to calculate because of the intangible considerations involved, but on the basis alone of the lower total plane mileage for military craft compared to commercial planes, it should be several thousand dollars per pound. This cost of weight contrasts to the direct cost of weight of from four to 30 cents a pound for the materials used in usual industrial electrical equipment. Accordingly, the efforts of designers of solenoid valves and similar air-borne equipment to reduce weight to the very minimum must be multiplied in proportion.

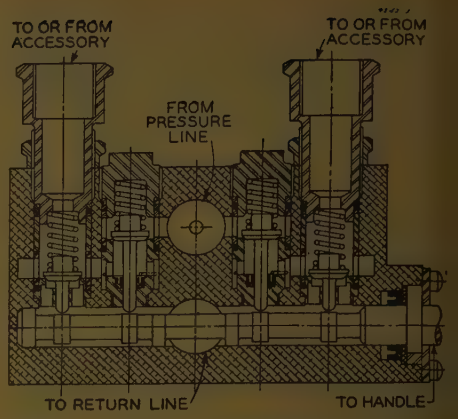


Figure 3. Section of manually operated unbalanced valve

Magnetic-circuit calculations indicate that the common iron-clad cylindrical solenoid is best adapted for this purpose. This shape also has the advantages of mechanical ruggedness, manufacturing simplicity, and relative invulnerability.

Besides the increased importance of weight there are two other design considerations which differ sufficiently from usual practice to be worth mentioning:

1. Valve operation is usually intermittent, most valves being used only once or twice for a minute or two during a flight.

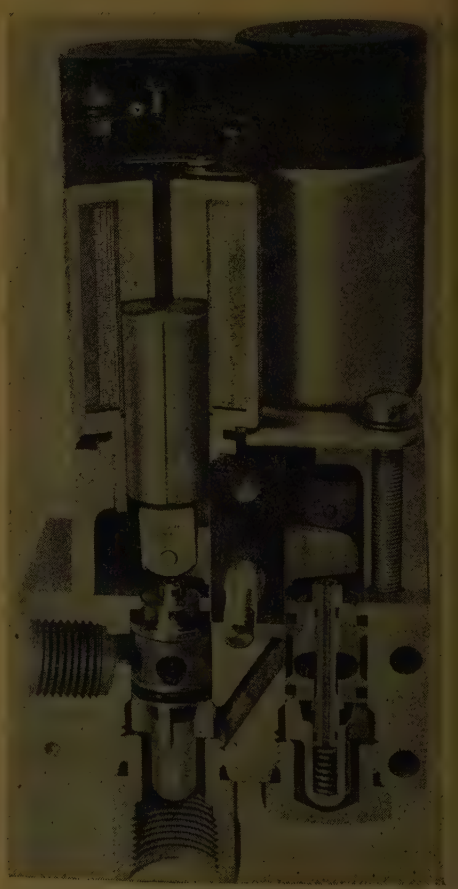


Figure 4. Cutaway model of partly balanced valve

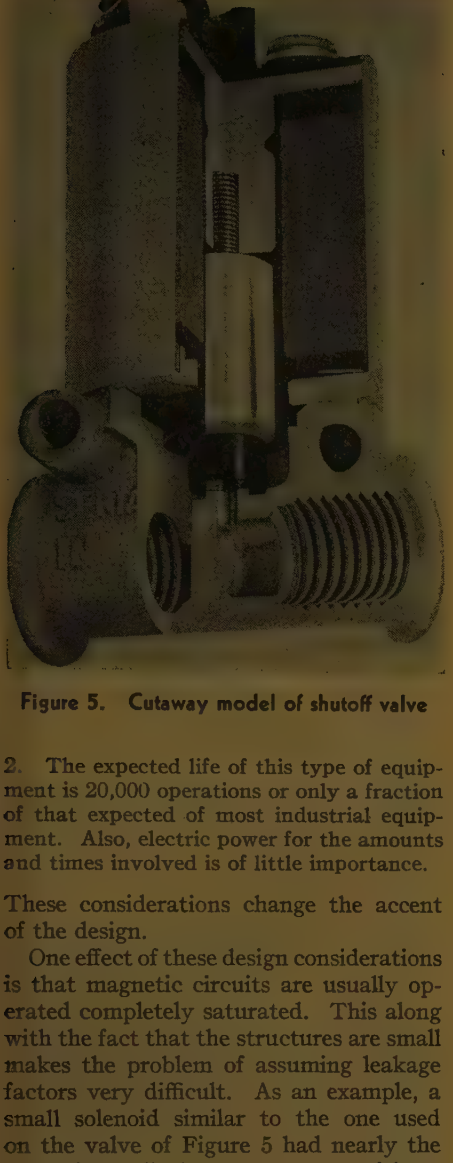


Figure 5. Cutaway model of shutoff valve

2. The expected life of this type of equipment is 20,000 operations or only a fraction of that expected of most industrial equipment. Also, electric power for the amounts and times involved is of little importance.

These considerations change the accent of the design.

One effect of these design considerations is that magnetic circuits are usually operated completely saturated. This along with the fact that the structures are small makes the problem of assuming leakage factors very difficult. As an example, a small solenoid similar to the one used on the valve of Figure 5 had nearly the same force–displacement curve without the outer iron shell as with it. This unit had been calculated carefully, but the error in assuming the leakage coefficients was so great that the outer shell had little magnetic value and, except for mechanical reasons, constituted nearly useless weight.

The engineer's first thought when confronted with the weight–duty cycle requirements along with an expected life of only 100 hours is to operate at higher than usual temperatures. Solenoids have been made using asbestos- and glass-insulated wire for operation at temperatures over 300 degrees Fahrenheit. Unfortunately, the temperature coefficient of resistance of copper is sufficiently high that the force is lowered as indicated by Figure 7 which is for an experimental glass-insulated solenoid at 0.125-inch plunger gap. When it is compared to the force–displacement curve 3 of Figure 8 for two poppets, it will be seen that a single poppet valve will operate when the solenoid is cold, will remain open for two or three minutes, and then will close automatically. After this time the valve will be entirely inoperative until it has cooled to approximately normal temperature.

Although this performance conforms with the normal duty cycle, it does not satisfy aircraft requirements in that the valve accidentally may be left in the circuit until hot in which case it will not operate if required to do so. One solution to this difficulty is to limit the temperature with a thermostatic overload switch such as is sometimes used on electric motors. A performance curve for a solenoid equipped with a thermostat switch set to open at 250 degrees Fahrenheit and reclose at 175 degrees Fahrenheit is given in Figure 9. This solenoid would operate the valve just during the time interval that the circuit was closed; during approximately 70 per cent of the

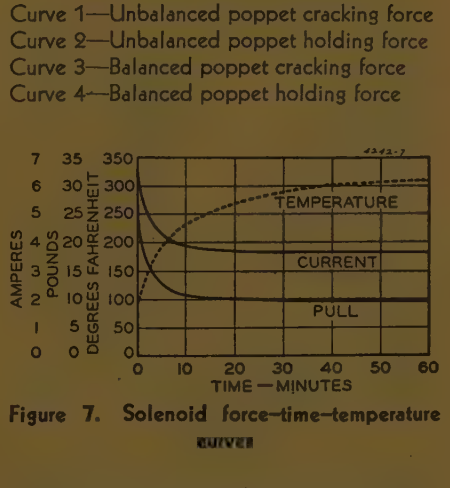


Figure 6. Hydraulic-valve-poppet operating-force curves for three-gallon-per-minute capacity (0.065-inch poppet travel)

Curve 1—Unbalanced poppet cracking force
Curve 2—Unbalanced poppet holding force
Curve 3—Balanced poppet cracking force
Curve 4—Balanced poppet holding force

Figure 7. Solenoid force–time–temperature curves

time it would be inoperative. This probably would be satisfactory for many applications, if a better arrangement were not available.

For optimum design of the unit, the valve-operating and solenoid forces should be matched as closely as possible. This may be accomplished at least roughly by:

1. Choosing the most suitable configuration of magnetic circuit.
2. Using a double-coil winding.
3. Combinations of these two principles.

For instance, referring to Figure 8, the valve force curve 3 can be matched more closely to a solenoid curve if a high ampere-turn winding is used to give the force curve 1 with the iron circuit designed to saturate at *E*. At point *F* on the solenoid curve, the low resistance coil is switched out and a high resistance coil substituted. In contrast, a normal unsaturated solenoid designed to give adequate force at point *A* would have a characteristic curve similar to curve 4. The heating and weight of such a design would be prohibitive.

The double-coil arrangement uses a high-power operating coil with comparatively few turns of heavy wire connected in parallel with a high-resistance holding coil wound with a large number of turns of fine wire. A pair of contacts operated by the solenoid plunger are connected to open-circuit the pulling coil when the plunger is at the inner limit of travel. Then, when the solenoid is first energized, the circuit resistance is low because the low-resistance coil is in the circuit so that with the plunger out the force is relatively high. When the plunger travel is completed the current is reduced by open-circuiting the pulling coil. Consequently, the force and heating are reduced.

Other circuits with somewhat different characteristics are available for accomplishing this same purpose. For instance, the two coils can be connected in series with the high-resistance holding coil short-circuited for the pulling stroke and open-circuited for the holding condition. This arrangement has the advantages of slightly lower heating while holding and improved contact conditions. The disadvantage is that the necessary inrush operating current is slightly higher. The parallel coil circuit was chosen for the solenoids described because of the lower inrush current.

Figure 8. Poppet and solenoid force–displacement curves

Curve 1—Solenoid plunger force at 80 degrees Fahrenheit
Curve 2—Solenoid plunger force at 250 degrees Fahrenheit
Curve 3—Force for two poppets
Curve 4—Typical unsaturated solenoid plunger force at 80 degrees Fahrenheit

The solenoid designed to operate the valve of Figure 4 has a flat-ended plunger and a double coil connected as in Figure 10. This is a three-position four-connection valve using four poppets and two solenoids with each solenoid operating two poppets. The characteristic curves for 80 degrees Fahrenheit and 250 degrees Fahrenheit are given in Figure 8. The operating coil is 394 turns of number 23-gauge copper wire and the holding coil is 2,150 turns of number 32-gauge wire. The room temperature resistances are 1.90 ohms and 152 ohms respectively, giving currents of 13.7 amperes and 0.171 ampere at 26 volts. At 250 degrees Fahrenheit the force with 0.065 plunger gap is 44 pounds with approximately 46 pounds hold-in force if the operating coil is not open-circuited. With only the holding coil in the circuit the holding force is 38 pounds. With the operating coil open-circuited as in normal use but with the holding coil energized, the ultimate temperature rise after several hours is about 60 degrees Fahrenheit. If the operating coil remained in the circuit, the solenoid literally would burn up in approximately three minutes.

The solenoid furnishes a driver that will operate the partly balanced valve of Figure 4 at just 250 degrees Fahrenheit. This is 30 degrees Fahrenheit higher than the highest operating temperature of 60 degrees Fahrenheit rise over 160 degrees Fahrenheit ambient so that some reserve driver force is available. However, the works represented by the areas under the respective force–displacement curves are matched so evenly that there is inadequate reserve to insure reliable operation under adverse conditions. This is taken care of by allowing the solenoid plunger to move 0.075 inch before it contacts the valve, thus permitting the additional kinetic work represented by the area *ABCD* of Figure 8 to be effective in opening the valve. This extra amount of driver work insures satisfactory switch action and valve operation under the most adverse conditions.

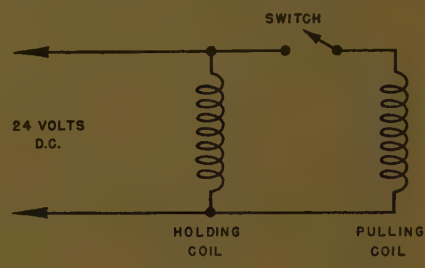
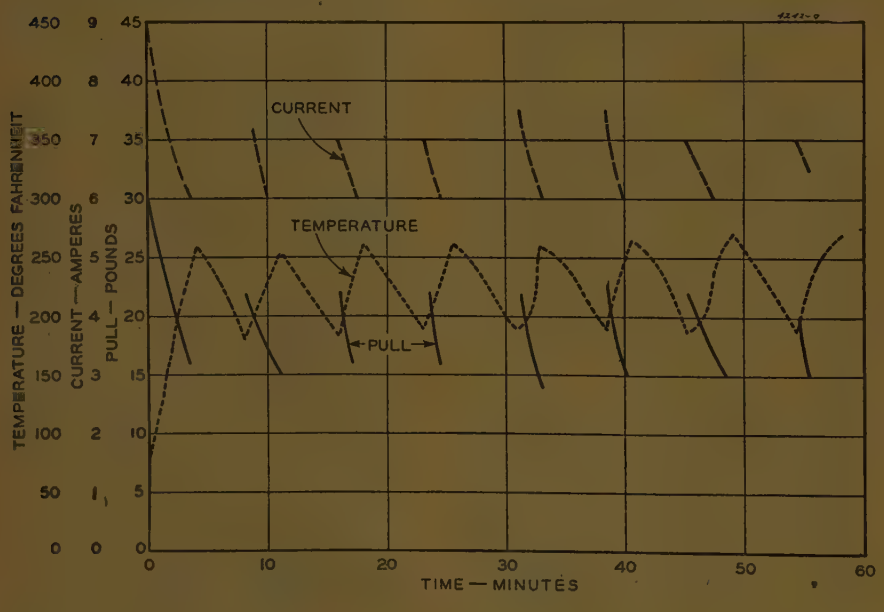


Figure 10. Circuit diagram of shunt double-coil solenoid winding

One interesting suggestion regarding means of reducing solenoid weight proposes to use aluminum wire instead of copper wire. Simple calculations show that in the coil itself a weight saving of approximately one half could be effected by this means. However, when the increase in weight of the enclosing iron magnetic structure caused by the larger volume of the aluminum is considered, the net difference in the weight of the whole structure is negligible. Moreover, insulated aluminum wire is difficult to obtain which is an important consideration at present.

Another argument concerns the winding of the coil. For 24-volt d-c service random winding would be entirely satisfactory, particularly with the new high-temperature nonscuffing enamels. However, the fact that approximately ten per cent more volume is required for a random winding than for a layer winding without extra insulation increases the frame weight about five per cent which is not justifiable.

Figure 9. Force–time–temperature curves for thermostatically controlled solenoids



Conclusions

The following general conclusions may be drawn as to the use and design of electric remotely operated hydraulic valves.

1. Considerable savings in installation weight may be effected by electric remote control of hydraulic valves, particularly when the accessory to be controlled is some distance from the cockpit.

2. It may be necessary to sacrifice simplicity and low valve cost to obtain the minimum over-all weight and dollar cost.

3. The analysis indicates that solenoid valves should be designed for low operating forces and efficient electrical and mechanical elements; not so much on account of the electric power requirements but to decrease heat dissipation and, consequently, size and weight.

References

1. Aircraft Accessory System, **T. B. Holliday**. Society of Automotive Engineers *Journal*, volume 51, July 1943, pages 234–40, 247.

2. The Case for Hydraulic Services, **R. Hadekel**. *The Aeroplane*, volume 66, January 21, 1944, pages 77–8.

3. Electromagnetic Devices (book), **Herbert C. Roters**. John Wiley and Sons, Inc., New York, N. Y., 1941.

4. Magnets (book), **C. R. Underhill**. McGraw-Hill Book Company, Inc., New York, N. Y., 1924.

5. Comparison of Hydraulic and Electrical Accessory System for Aircraft, **W. C. Trautman, R. E. Middleton**. Society of Automotive Engineers *Transactions*, volume 52, March 1944, pages 94–9.

A New High-Frequency Capacitor

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Synopsis: Limitations of the ordinary capacitor at high frequencies have been in part overcome by a new three-terminal feed-through capacitor having a low impedance over a wide frequency range, thus enabling improved by-passing or filtering over this wide range of frequencies.

ONE of the common uses of capacitors is by-passing high-frequency current from circuits in which direct or low-frequency current is flowing. The frequency range over which it is effective as a by-pass depends upon the relative impedance of the capacitor and the impedance across which it is connected.

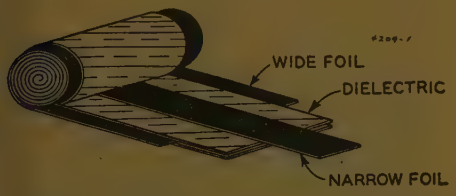


Figure 1. Isometric drawing of nearly rolled Hypass capacitor showing electrode positions



Figure 2. Completed Hypass capacitor with leads attached

The impedance of a capacitor at a given frequency is determined by its capacitance, inductance, and resistance. At low frequencies the impedance is largely determined by the capacitance, but at high frequencies and thus in most by-passing applications, lead inductance and the inherent inductance of the capacitor become the controlling factors.

Usually, capacitors for by-pass use are so chosen that series resonance occurs at the frequency where the most complete elimination of undesired high-frequency current is needed. By increasing the ratio of C to L, the frequency range over which the capacitor is effective as a by-pass can be increased. Mechanical considerations (namely, the necessity for making connections to the capacitor) and the inherent inductance of the capacitor limit the frequency range over which low impedance can be obtained by this approach.

The use of capacitors in parallel having different impedance–frequency characteristics always results in a region of high impedance where the combination becomes antiresonant. Thus, this commonly used method of by-passing over wide frequency ranges may be ineffective unless the region of antiresonance is chosen where no by-passing is needed.

A new approach to the wide-band by-passing problem is the Hypass capacitor which uses a unique design to provide an extremely broad range of low impedance extending into the ultra-high-frequency range.

Construction of the Hypass Capacitor

"Hypass" designates a three-terminal feed-through capacitor which lacks the usual resonant characteristics of ordinary capacitors. This is accomplished by operation of the capacitor as a transmission line and by a physical design which allows extremely low inductance in external connections.

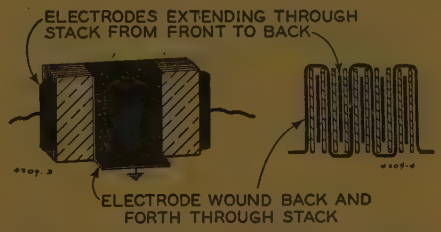


Figure 3. Stacked mica Hypass capacitor

Figure 4. Cross-section of mica Hypass capacitor

The Hypass capacitor may be constructed with the same conductor and dielectric materials as conventional types, but the internal connections to the capacitor plates and the arrangements of the plates and dielectric differ from the usual type. In its usual form the Hypass capacitor has two long foil electrodes of unequal width separated by a dielectric of intermediate width. The foils and dielectric may be wound or rolled so that the wider foil extends beyond both edges of the dielectric as shown in Figures 1 and 2. The narrow foil is centered in respect to the edges of the dielectric and connected at only one end to a terminal, usually a metal container which may be grounded. Each edge of the extending wider foil is then made essentially a unipotential surface by soldering or other means by the techniques employed in conventional "noninductive" capacitors. Leads are connected to the two resulting equipotential surfaces to carry the low-frequency or direct current through the capacitor. The current-carrying capability of the Hypass capacitor may be increased by inserting an auxiliary conductor through the capacitor without affecting adversely the frequency characteristics.

The Hypass capacitor must be inserted in a circuit just as a low-pass filter would be connected. Its usual mechanical construction lends itself to an extremely low-inductance connection to ground while inductance in the other connections enhances the filtering and thus the by-passing effect.

Many modifications of the construction outlined above are possible. Hypass capacitors have been constructed with flat dielectric sheets, such as mica, with the wider foil replaced by strips of foil extending through the stack and with the narrow foil folded back and forth pro-

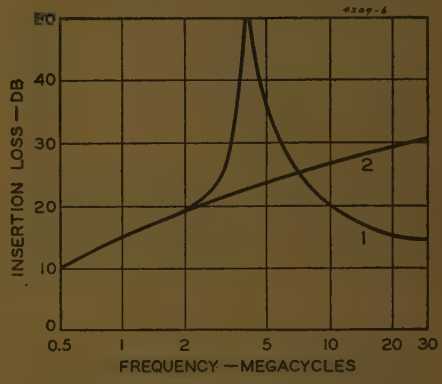


Figure 6. Insertion loss of a Hypass capacitor versus a conventional capacitor

Curve 1—Conventional 0.1 microfarad
Curve 2—Hypass 0.1 microfarad

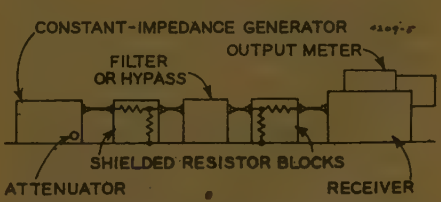


Figure 5. Insertion-loss measuring apparatus used for determining Hypass characteristics

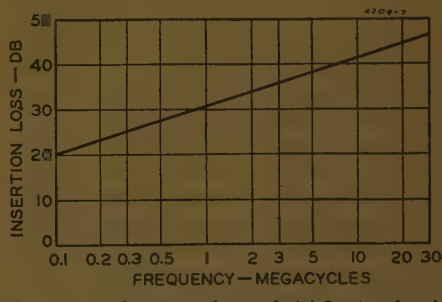


Figure 7. Insertion loss of 14.2-microfarad Hypass capacitor

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Analysis of High-Frequency Ignition Circuits

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THE modern high-voltage magnetos installed on a 2,000-horsepower aircraft engine may not resemble externally the crude induction coil used by Lenoir in 1860 to provide ignition for his gas engine. Yet, both make use of the same method to generate a transient voltage high enough to fire the spark plug. A current is first established in a primary winding surrounding a core of magnetic material and then suddenly interrupted. This interruption produces a high rate of change of flux through a secondary coil wound with a large number of turns, thus inducing a high voltage that is transmitted to the spark plug.

While their principle of operation has remained basically unchanged, the design of ignition systems has made considerable progress through the years. At the turn of the century, Simms and Bosch combined the current generator with the induction coil to make the first high-voltage magneto. Since then, new materials have become available—plastics, lightweight alloys, Alnico—and have been incorporated into improved mechanical designs to give the high-voltage magneto its present performance and reliability.

With the recent advances in aircraft-engine design, yielding higher-power outputs at higher altitudes, certain limitations of the conventional high-voltage ignition system have become of great concern to the ignition engineer. Not only is it necessary to generate and distribute higher and higher voltages at extreme altitudes, where air becomes a poor insulator, but the higher energies at the spark plug have contributed to accelerate the erosion of its electrodes and decrease its life. Fuels with high lead content, and cold running plugs, both essential prerequisites for getting more power out of a given engine, have increased the danger of spark-plug fouling, causing considerable trouble in operation.

Most of these problems have been brought under control by "fixes," and by refinements of conventional ignition systems. All persons connected with aircraft ignition have been wondering how much longer it would be possible to obtain the desired results without making a radical departure from existing systems. At the present time several manufacturers are developing new types of ignition systems that promise to get around most of the present limitations.

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Low-Voltage High-Frequency Ignition Systems

Several of the proposed systems combine high frequency with distribution at low or medium voltages, offering such outstanding advantages as:

1. Operation at very high altitudes without need for supercharging.
2. Freedom from the effects of fouling.
3. Reduced plug erosion.

Most of these systems utilize the same basic high-frequency circuit: a capacitor, charged to a predetermined voltage considerably lower than the plug voltage, is suddenly discharged through the primary winding of an oscillation transformer. The secondary winding is connected to the spark-plug electrodes. This circuit is nothing more than the original Tesla-coil circuit, invented by Nikola Tesla about 1890, for producing high voltages at high frequencies. While the methods of charging and discharging the capacitor, the disposition of the components, and the values of the circuit constants vary widely from one system to another, the analysis of the basic Tesla-coil circuit seems to be of general interest, and will be the main object of this paper.

Several scientists, such as Boynton[1] and Drude,[2] have given complete mathematical solutions of circuits of this type, the former having checked experimentally the results of his theoretical investigation. However, these results may be too com-

gressing through the stack as shown in Figures 3 and 4. The resulting Hypass capacitor has characteristics similar to the previously described type.

Performance

The behavior of Hypass capacitors was evaluated by means of insertion-loss measuring apparatus developed for low-pass filter measurement. This equipment consists of a signal generator, a detector, and decoupling networks as shown in Figure 5. Insertion loss was measured by comparing the input required to produce a given deflection of the detector with and without the Hypass capacitor inserted between the decoupling networks. The impedance level was arbitrarily fixed at ten ohms.

Curve 1, Figure 6, shows insertion loss versus frequency characteristics for a two-terminal noninductive capacitor typical of the best commercially available. Curve 2, Figure 6, shows a Hypass capacitor of the same low-frequency capacitance. Figure 7 shows the behavior of an experimental Hypass capacitor of 14.2 microfarads at 60 cycles.

The Hypass capacitor derives its favorable frequency characteristics from a decrease in the effective capacitance as a function of the increase in frequency. That this effect is not due to a change in the dielectric constant of the insulation was determined by the use of mica and polystyrene with measurements between 200 kilocycles and 30 megacycles. Thus, the capacitor represented in Figure 7 still behaves as a capacitor at 20 megacycles, although it is equivalent at this frequency to a perfect capacitor having a capacitance only a little larger than 0.1 microfarad.

Analysis on the basis of the device operating as a transmission line accounts for the seeming decrease in capacitance with increasing frequency. Its impedance is similar to a very low-impedance transmission line having high capacitance per unit length. Changes with frequency of the capacitance, inductance, conductance, and resistance per unit length are secondary and additional factors in determining the by-pass characteristics. Insertion loss versus frequency characteristics may be altered by changing the relative dimensions or the materials used in the Hypass capacitor. Qualitative evidence indicates that Hypass capacitors are effective for by-passing at frequencies at least 150 megacycles.

Applications

Among the obvious applications of Hypass capacitors are the suppression of radio noise over an extremely wide frequency range, the by-passing of multiband radio-receiver and transmitter circuits, and the use of Hypass capacitors as circuit elements in conventional filters and networks to obtain unique characteristics. Like low-pass filter sections Hypass capacitors may be connected in cascade with the marked improvement in filtering or by-passing behavior. Hypass capacitors may in certain cases be connected in parallel apparently without the troublesome antiresonance effects obtained as described in the introduction.

plete to be of much practical use to the engineer, at least in their original form. An "exact" formula, taking into account all parameters, can at best be used to calculate the performance of a device that has already been designed. The engineer, starting from scratch and faced with the problem of designing the device the best he knows how, would prefer to proceed by steps: as a starting point, a rather oversimplified theory, taking into account only the most fundamental factors, but easy to visualize, will allow him to study the basic limitations of a design, and see what they mean in terms of

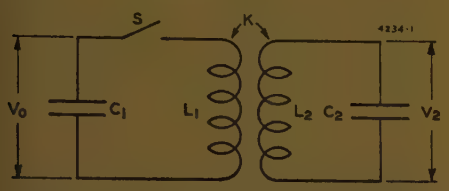


Figure 1. Basic oscillation transformer circuit

inches, materials, wire sizes, and numbers of turns. Once a preliminary layout has been made, secondary factors can be introduced one at a time as simple corrections, each one giving a closer approximation to the final result. It may be objected that such an approximate method is less accurate than an exact method. But an exact mathematical method, applied to an engineering problem, is only as good as the unavoidable assumptions on which it is based, and certainly not beyond the accuracy of the data. The main advantage of a theoretical analysis resides in its ability to break down a complex problem into its simplest elements or notions, simple enough so that each one can be visualized. This is nothing more than the method advocated by Descartes, pioneer of analysis and visualization.

Analysis of Circuit Without Losses

This section is a mathematical study of the transient generated by discharging a capacitor C_1, charged initially to a voltage V_0, through the primary winding of an oscillation transformer, the secondary winding being connected to a capacitive load C_2. Let L_1 and L_2 be the inductances of the primary and secondary circuits respectively and K the coefficient of couling between the two windings, defined as $K=L_{12}/\sqrt{L_1L_2}$, where L_{12} is the mutual inductance between the circuits. All losses will be neglected and all circuit elements assumed to be linear (C_1, C_2, L_1, L_2, L_{12} constants). C_2, the secondary load, represents the sum of the plug capacitance, lead capacitance, and a lumped capacitance equivalent to the distributed winding capacitance. Figure 1 shows this circuit, the device for discharging the capacitor C_1 being represented schematically by a switch S.

The final purpose of the ignition system is to charge C_2 to a voltage high enough to break down the spark-plug gap, and this should be done as efficiently as possible: the circuit should be proportioned so that a large percentage of the energy initially stored in C_1, is transferred to the secondary capacitance C_2. The minimum electric power required to supply ignition to a 2,000-horsepower engine is equivalent to somewhat less than $^1/_{100}$ of a horsepower, so that, even with a very low efficiency, an ignition system would decrease the available engine power only by a negligible amount. However, if more joules must be generated per second in the ignition system, the generating units must be bigger and heavier, and weight is at a premium on all aircraft equipment.

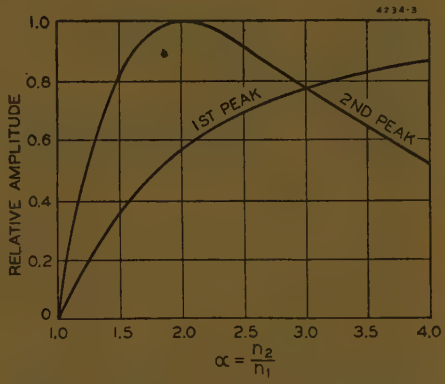


Figure 3. Effect of ratio of frequencies of components upon first and second peaks of secondary-voltage wave

An ideal equivalent circuit as described in the foregoing is, of course, a pure abstraction, but its practical interest is undeniable: the values of circuit constants that will give optimum operation (highest efficiency of energy transfer) in this circuit are good approximations of the values for optimum operation in an actual case, where the output is reduced by unavoidable losses.

The differential equation of this circuit set up in the usual manner can be brought to the quadratic form:

$$(L_1L_2-L_{12}^2)p^4+\left(\frac{L_1}{C_2}+\frac{L_2}{C_1}\right)p^2+\frac{1}{C_1C_2}=0 \quad (1)$$

Regardless of boundary conditions, it shows that all natural oscillations of this circuit include components of two frequencies n_1 and n_2 determined by

$$4\pi^2n_1^2=\frac{\frac{1}{L_1C_1}+\frac{1}{L_2C_2}-\sqrt{\left(\frac{1}{L_1C_1}+\frac{1}{L_2C_2}\right)^2-4\frac{1-K^2}{L_1C_1L_2C_2}}}{2(1-K^2)} \quad (2)$$

$$4\pi^2n_2^2=\frac{\frac{1}{L_1C_1}+\frac{1}{L_2C_2}+\sqrt{\left(\frac{1}{L_1C_1}+\frac{1}{L_2C_2}\right)^2-4\frac{1-K^2}{L_1C_1L_2C_2}}}{2(1-K^2)} \quad (3)$$

These two frequencies are different from the natural frequencies of the two circuits taken separately and always frame them.

In order to determine the amplitudes and phase angles of these two components, the boundary conditions should be introduced. If it is assumed that C_1 to be charged to a voltage V_0 for $t<0$, and the switch S to be closed at $t=0$, the expression for the voltage across the capacitor C_2 is

$$v_2=\frac{L_{12}C_1V_0}{\sqrt{(L_1C_1-L_2C_2)^2+4K^2L_1C_1L_2C_2}}\times(\cos 2\pi n_1t-\cos 2\pi n_2t) \quad (4)$$

Under no conditions can v_2 be larger than

$$V_2=V_0\sqrt{\frac{C_1}{C_2}} \quad (5)$$

as, for $v_2=V_2$, all the energy initially stored in C_1 has been transferred to C_2, so that the currents through L_1 and L_2 and the voltage across C_1 are simultaneously zero.

The quantity v_2/V_2, representing the ratio of actual secondary voltage to maximum possible voltage, is thus representative of the efficiency of the circuit in converting electrostatic energy in C_1 into electrostatic energy in C_2. If $m=L_1C_1\div L_2C_2$, this ratio can be written

$$\frac{v_2}{V_2}=\frac{1}{\sqrt{1+\frac{(m-1)^2}{4K^2m}}}\times\frac{\cos 2\pi n_1t-\cos 2\pi n_2t}{2}=U\times\frac{\cos 2\pi n_1t-\cos 2\pi n_2t}{2} \quad (6)$$

The second factor,

$$\frac{\cos 2\pi n_1t-\cos 2\pi n_2t}{2}$$

represents the wave shape of the secondary voltage. Figure 2 shows this wave shape, and Figure 3 the amplitudes of the first and second peaks of this wave, plotted against the ratio of natural frequencies $\alpha=n_2/n_1$. Similar curves could be drawn for the third, fourth peaks, and so on. For $\alpha=2$, 4, 6, etc., one of these peaks will be 1, the two components being in phase and maximum at the same time.

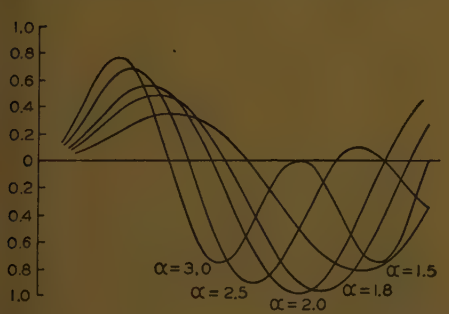


Figure 2. Theoretical wave shapes of secondary voltage

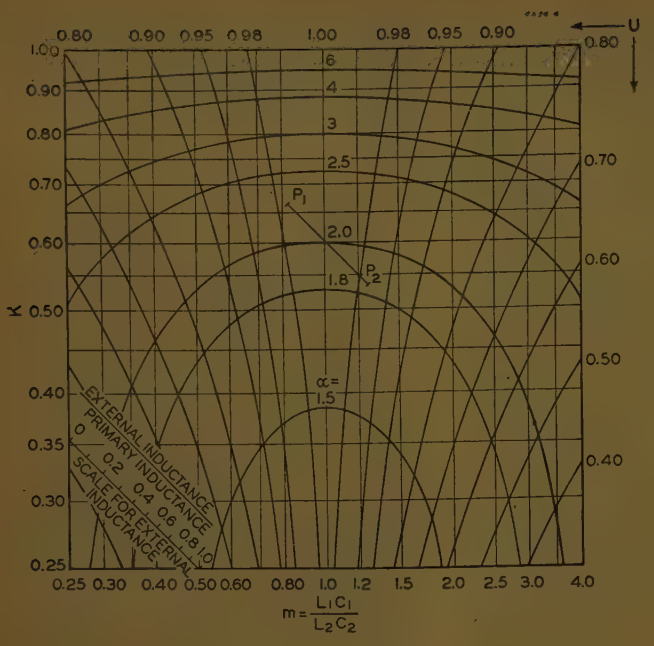


Figure 4. Chart for determination of secondary voltage

The first factor, U, is a measure of the amplitude of the secondary wave and also is at the most equal to 1.

The quantities U and α, determining the amplitude and wave shape of the secondary voltage, are functions of only two parameters, the coupling coefficient K between the circuits, and the quantity $m = L_1C_1/L_2C_2$ equal to the square of the ratio of natural frequencies of the secondary and primary circuits taken separately. These functions are represented on the chart of Figure 4.

By studying the chart, and the curves of Figure 3, the following conclusions may be reached:

1. All the energy initially in the primary capacitor will be transferred to the secondary capacitor if the primary and secondary circuits, when taken separately, have equal natural frequencies, and if the coefficient of coupling between the two circuits is 0.60.

2. These two conditions are not critical, only a slight reduction in peak secondary voltage resulting from a fairly wide departure from the ideal conditions.

The first condition gives maximum amplitude for the secondary voltage, and, combined with the second condition of 0.60 coupling coefficient, makes the higher natural frequency of the system double the lower. Maximum efficiency also could be obtained with frequency ratios of 4, 6, etc., but the corresponding coupling coefficients, 0.882, 0.956, etc., may be higher than can be practically incorporated in this type of transformer.

Effect of Primary-Lead Inductance

A certain amount of external inductance in the primary circuit, contributed by leads, capacitor, and so forth, may be appreciable when compared to the transformer primary inductance, so that its effect cannot be neglected.

If K' is the coupling coefficient of the transformer, L_1' the inductance of its primary winding, L_1'' the external inductance (Figure 5), the circuit constants can be written:

$$L_1 = L_1\left(1+\frac{L_1''}{L_1'}\right) \qquad (7)$$

$$K = \frac{K'}{\sqrt{1+\frac{L_1''}{L_1'}}} \qquad (8)$$

If, on the chart of Figure 4, P_1 represents the operating point of the transformer without external inductance (K' and $L_1'C_1/L_2C_2$) a simple construction makes it possible to determine the operating point P_2 for the circuit with the external inductance L_1'' in series with the primary inductance. A 45-degree line is drawn through P_1, and a distance scaled along the line corresponding to the ratio L_1''/L_1' of external to primary inductances. The scales for m and K on the chart have been chosen so that the point P_2 thus determined represents operation with external inductance.

It is interesting to notice that a fairly large percentage of external inductance can have negligible effects and that, in some cases, an increase in external inductance actually may raise the secondary voltage.

Effect of Resistance Losses

So far, only an ideal "no-loss" circuit has been considered. Such an analysis will not predict the actual output of an oscillation transformer, but an upper limit that can be approached only by careful design. It gives, however, some very helpful information as to the optimum relationships between the circuit constants, and the decrease in output to be expected when these constants are varied from their optimum values.

Many kinds of losses will cause the actual output voltage to be lower than the values given by a "no-loss" analysis:

1. Corona losses.
2. Dielectric losses.
3. Eddy-current losses in all metallic parts surrounding the high-frequency circuit, such as shielding.
4. Hysteresis losses in magnetic materials.
5. Voltage drop across the device used for discharging the capacitor.
6. Resistance losses in circuits.
7. Losses due to leakage resistance across the terminals.

Only losses due to resistance of the circuits will be briefly studied in this paper, not because all other losses can be neglected, but rather because only resistance losses lend themselves to a fairly simple mathematical analysis. While the effect of losses such as those due to eddy currents and hysteresis can be evaluated roughly by analysis, actual tests afford a simpler and more reliable way to study them.

Equation 1 can be modified by adding terms representing the effect of resistances R_1 and R_2 of the primary and secondary circuits respectively:

$$(L_1L_2-L_{12}^2)p^4+(L_1R_2+L_2R_1)p^3+\left(\frac{L_1}{C_2}+\frac{L_2}{C_1}+R_1R_2\right)p^2+\left(\frac{R_1}{C_2}+\frac{R_2}{C_1}\right)p+\frac{1}{C_1C_2}=0 \qquad (9)$$

It should be noted that R_1 and R_2 are a-c resistances, usually higher than the d-c resistances, unless the wire sizes used are small enough in comparison to the depth of penetration to make skin effect negligible.

In a study of general scope such as the present, it is more desirable to express

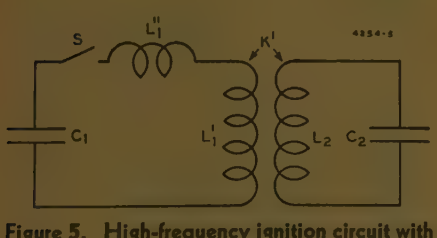


Figure 5. High-frequency ignition circuit with external inductance

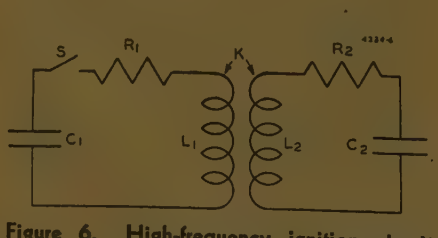


Figure 6. High-frequency ignition circuit including winding resistances

the resistances in "per unit" values than in ohms. The amount of damping in each circuit can be represented by the dimensionless quantities:

$$\rho_1 = \frac{R_1}{2\sqrt{\frac{L_1}{C_1}}} \quad (10)$$

$$\rho_2 = \frac{R_2}{2\sqrt{\frac{L_2}{C_2}}} \quad (11)$$

ρ_1 and ρ_2 being, for each circuit taken alone, the ratio of actual resistance to the resistance for critical damping.

Equation 9 can now be written:

$$(1-K^2)p^4+2\left(\frac{\rho_1}{\sqrt{L_1C_1}}+\frac{\rho_2}{\sqrt{L_2C_2}}\right)p^3+\left(\frac{1}{L_1C_1}+\frac{1}{L_2C_2}+\frac{4\rho_1\rho_2}{\sqrt{L_1C_1L_2C_2}}\right)p^2+\frac{2}{\sqrt{L_1C_1L_2C_2}}\left(\frac{\rho_1}{\sqrt{L_2C_2}}+\frac{\rho_2}{\sqrt{L_1C_1}}\right)p+\frac{1}{L_1C_1L_2C_2}=0 \quad (12)$$

In all practical cases, the amount of damping will be small, $\rho_1 \ll 1$ and $\rho_2 \ll 1$. If the phase angles, which are small and would introduce only amplitude errors of the second order in the neighborhood of the peaks are omitted, the voltage output of the transformer can be written:

$$\frac{v_2}{V_2} = U \times \frac{\epsilon^{-\alpha_1 t}\cos 2\pi n_1 t - \epsilon^{-\alpha_2 t}\cos 2\pi n_2 t}{2} \quad (13)$$

U, n_1, and n_2 differing only by second order quantities from the values found previously in the "no-loss" case. The decrement factors, $-\alpha_1$ and $-\alpha_2$, are the real parts of the roots of equation 12.

In order further to simplify the problem, it will now be assumed that $L_1C_1 = L_2C_2$, this being a desirable condition for operation ($U=1$). The circuit damping coefficients ρ_1 and ρ_2 now enter into equation 12 only as their sum $\rho_1+\rho_2$, so that α_1 and α_2 will be functions of $\rho_1+\rho_2$. In other words, a certain per unit damping will have exactly the same effect upon the output, whether it is in the primary or the secondary circuit.

If the coupling between circuits has been chosen for optimum wave shape, the maximum voltage will be reached approximately when $\cos 2\pi n_1 t = -1$ and $\cos 2\pi n_2 t = 1$, so that v_2/V_2 will have a peak value:

$$\frac{v_{2m}}{V_2} = \frac{\epsilon^{-\alpha_1 t}+\epsilon^{-\alpha_2 t}}{2} \quad (14)$$

If all quantities beyond the first order are neglected:

$$\frac{v_{2m}}{V_2} = 1\frac{(\alpha_1+\alpha_2)t}{2} \quad (15)$$

The sum of the roots of equation 12 is $-2(\alpha_1+\alpha_2)$, the imaginary parts canceling out as the roots form two sets of conjugate complex quantities. It is thus possible to evaluate $\alpha_1+\alpha_2$ without solving equation 12, the sum of the roots being equal to the negative value of the ratio of the coefficient of p^3 to the coefficient of p^4.

Thus, from the coefficients of equation 12:

$$\frac{v_{2m}}{V_2} = 1-\frac{(\rho_1+\rho_2)t}{2(1-K^2)\sqrt{L_1C_1}} \quad (16)$$

t, is of course the time at which the peak occurs.

The most interesting case to study is the optimum case determined in a previous section, for $m=1$ and $K=0.60$. Without losses, the maximum voltage would occur when $2\pi n_1 t = \pi$. If a small amount of damping is added, the displacement of the peaks due to this damping can be neglected, and the effect of resistance upon peak secondary voltage can be computed by equations 16 and 2, the latter being used to determine the lower natural frequency n_1.

$$\frac{v_{2m}}{V_2} = 1-3.11(\rho_1+\rho_2) \quad (17)$$

Conclusions

It is hoped that this study will help elucidate some of the fundamental concepts connected with this type of circuit. In particular, it is interesting to note that there are no critical conditions of resonance in this circuit, such as there would be if it were operated under steady-state rather than transient conditions. Also, the fact that a very high coefficient of coupling is not necessary for satisfactory operation may sound surprising at first.

The great majority of electrical problems deal with steady-state conditions, so that, whenever transient phenomena have to be considered, as in the present case, different methods of analysis and special measuring equipment are required. Fast-recurring transients usually can be studied with the aid of a cathode-ray oscilloscope, providing the extra load on the system does not change materially the transient being studied. In high-frequency ignition circuits, it is often impossible to satisfy this condition, and it may even be difficult to obtain an oscilloscope of sufficient writing speed.

There are, however, certain simple relations between the natural frequencies of oscillation of coupled circuits with low damping and the resonant frequencies of such a circuit excited by a sine-wave oscillator. For instance, if the input impedance of the circuit of Figure 1 is measured at the switch S for various frequencies, the curve of impedance versus frequency will show a maximum for a frequency approximately equal to the natural frequency of the secondary circuit taken alone and two minima corresponding to the two component frequencies of the transients studied in previous sections. The relative amplitudes of the maximum and minima give an indication of losses in the circuits. Such measurements can be made with standard equipment: a radio-frequency oscillator, a vacuum-tube voltmeter and a hot-wire ammeter. Even though they have to be taken at low flux densities, they give some interesting data that can be of great help in the test of high-frequency ignition circuits. The development of such test methods and of special measuring equipment should parallel the development of the circuits, to give a better understanding of their operation and thus guide the engineer responsible for their design.

References

1. A Quantitative Study of the High-Frequency Induction Coil, W. P. Boynton. *Philosophical Magazine*, volume 46, September 1898, pages 312–38.

2. Über Induktive Erregung Zweier Elektrischer Schwingungstreise Mitt Anwendung auf Perioden und Dämpfungsmessung, Tesla-transformatoren und Drahtlose Telegraphie, von P. Drude. *Annalen der Physik* (Leipzig, Germany), volume 13, 1904, pages 512–61.

3. Induction Coil—Theory and Applications (book), E. Taylor Jones. 1932. Pages 177–89.

4. Principles of Electric-Wave Telegraphy and Telephony (fourth edition), J. A. Fleming. 1919. Pages 232–64.

5. Alternating and Transient Currents in Coupled Electrical Circuits, F. E. Pernot. University of California publications in engineering, volume 1, number 8, September 26, 1916, pages 161–260

Inherent Overheating Protection of D-C Aircraft Motors

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Synopsis: Inherent overheating protection is undoubtedly one of the major developments in the fractional-horsepower-motor field in the last decade. Thermal protection is now advocated by some for aircraft motors, and many thermally protected motors are now in aircraft service. In this paper are reported the results of a number of laboratory tests taken on a typical aircraft motor. Tests were taken on a 1/15-horsepower 7,500-rpm d-c motor; loads ranged from locked rotor to the minimum running load sufficient to trip the thermostat. Ambient temperatures ranged from −58 to +78 degrees centigrade. Locked-rotor and running-overload temperature tests were also taken at a density altitude of 42,000 feet. Temperatures of the winding, frame, brush holders, and bearings were measured carefully under all conditions mentioned. Satisfactory protection, without premature tripping, was invariably achieved.

IN 1925, introduction of thermostatic protection to domestic flatirons revolutionized the flatiron industry. In 1928, disk-type thermostats were built into a portable-tool motor to provide built-in thermal protection. Two or three years later, thermostats were used to provide built-in overload protection for single-phase induction motors. Since that time, a quiet revolution has been taking place in the fractional horsepower motor industry, in the form of an ever-increasing use of these built-in thermal protective devices. By 1939, the Underwriters' Laboratories had become interested, and in June of that year, after conferring with electrical manufacturers making and using such devices, the Underwriters' Laboratories wrote a standard for inherent overheating protective devices; this standard was incorporated as an appendix to the Underwriters' standard for industrial control equipment.

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If the present trend to thermal protection of motors continues, nearly all domestic fractional-horsepower motors will eventually be provided with inherent overheating protection.

Just because the merit and utility of inherent overheating protection for industrial fractional-horsepower motors have been indubitably established, it does not necessarily follow that similar protection is necessary, or even desirable, for aircraft motors. Requirements of motors for aircraft service differ so materially from the requirements of motors for industrial or domestic service that the worth of thermal protection for aircraft motors has to be examined in a different light. T. B. Holliday, in a recent article,[6] has pointed out a number of reasons why thermal protection of aircraft motors is desirable. Some of the reasons given by him are:

1. To prevent motor burnouts due to temporary or permanent failures of the driven device. If motor burnout is prevented, the motor itself does not have to be replaced, and as the driven device can often be repaired, a new motor—frequently not available in remote places—is not required.

2. To prevent burnouts due to improper setting or functioning of limit switches. Hydraulic systems, he points out, have relief valves which prevent damage to the system in the event of jamming, and why shouldn't electric motors be similarly protected?

3. Even turret motors should use protected motors; in the event of damage to a part of the track, it is better, Colonel Holliday argues, to save the motor, and thereby allow it to be used to traverse the good section of the track, rather than have the motor burn out completely.

4. A nonprotected landing-gear motor may burn out in an attempt to put down landing gear in which the grease has stiffened excessively, whereas a thermally protected motor may, by several starts and stops, get the gear down.

Opponents of thermal protection contend that an aircraft motor should be kept energized "to the last ounce of effort"; the "last shot" may mean the difference between life and death to the crew. It is argued that an overload device may trip at a crucial moment, whereas perhaps another second or two of operation of the motor would have saved the emergency situation.

Whatever may be the merit in these contentions, it is beyond the scope of this paper to attempt to analyze and weigh all the controversial aspects as to whether thermal protection should or should not be used for aircraft motors. Rather, the aim of this paper is to present a picture as to just how thermal protection generally functions. It was felt that such a picture could best be presented by reporting specific test data on a representative aircraft motor equipped with inherent overheating protection.

Motor-Thermostat Combinations Tested

Following are the name-plate specifications for the motor selected for this test:

Outside diameter of frame, inches	2 5/16
Volts	24
Horsepower	1/15
Rpm	7,500
Amperes at full load	3.75
Wound	Shunt
Manufacturers frame number	G-234
Open or enclosed	Open

A shunt-wound motor was chosen, because that type is more difficult to protect than a series motor.

Disk-type thermostats were used for all tests. While this device is familiar to many, a brief description of the disk principle is in order. The disk-type thermostat has as its single moving element a bimetallic disk. This disk has a dished shape, with the high-expansive side of the bimetal on the concave side of the disk at normal temperature. As the disk is heated, the bimetal on the inside of the disk tries to expand but is restrained from doing so by the concave shape of the disk, for the other side, being of nonexpansive metal, does not expand similarly; as the temperature of the disk is increased, stresses are set up tending to buckle the disk, and as soon as its

Table I. Locked-Rotor Test on the Enclosed Motor, at 26 Volts, and at an Ambient Temperature of 26 Degrees Centigrade

	Amp Just Before Thermostat Opens	On Time, Sec	Off Time, Sec	Peak Temp, Armature Winding	Temp of Front Bearing
First cycle of thermostat	21.6	4.5	3.5	68	26
Second cycle of thermostat	20.8	1.3	3.6	72	
Third cycle of thermostat	20.6	1.3	4.0	78	
Fourth cycle of thermostat	20.4	1.3	4.6	84	
Fifth cycle of thermostat	20.2	1.4	5.1	90	
Sixth cycle of thermostat	20.0	1.3	5.3	94	34
After 10 min, lapsed time	16.8	1.8	20.5	172	64
After 30 min, lapsed time	16.8	1.8	33.4	168	99
After 64 min, lapsed time	16.7	2.0	34.4	164	100

temperature has reached a certain predetermined value, the disk snaps suddenly over to the other position. When the disk cools to a definite temperature, it will snap back to its original shape. It is to be noted that this disk operates when, and only when, it reaches a certain fixed temperature, called the opening temperature. This temperature may be reached as a result of heat received from the motor, by heat generated in an auxiliary heater within the thermostat, or by both. All of the thermostats used in the tests reported herein contain an auxiliary heater, as well as the disk.

Three motor–thermostat combinations were tested:

1. Totally enclosed with a thermostat mounted on the end of the front bracket, as shown in Figure 1.

2. As a normal open motor, with a "silk-hat" type thermostat mounted on the motor frame, as shown in Figure 2.

3. As an open motor with a thermostat inside the front bracket near the commutator.

The first arrangement was chosen as representing a practical arrangement for a totally enclosed motor. While the normal horsepower rating of an open motor is reduced by enclosing, it was felt that better comparative results would be obtained if the same motor were used for all tests; in reviewing the tests, it should be borne in mind that the motor would normally be rated at less than $^1/_{15}$ horsepower when enclosed. A "silk-hat" type thermostat, as shown in Figure 2, is a normal mounting arrangement for an open motor. The third arrangement—a thermostat inside the motor—was selected for these tests because it was felt that the inside of the motor was the most logical location from a thermal standpoint. Therefore, this arrangement was tried experimentally, although it was recognized that it would not be practicable in production motors because of space considerations.

Tests on these three motor–thermostat combinations afford a good comparison of the effect of thermostat location, and illustrate the general characteristics of inherent overheating protection afforded by a disk-type thermostat.

Temperature Measurements

For locked-rotor tests which were taken first, thermocouples were soldered to the armature winding at each end; one thermocouple was tucked into an interstice in the armature end winding in order to compare temperatures with those obtained by the soldered couples. Similarly, thermocouples were also soldered to each of the field coils, and to the commutator end of a brushholder. Additional thermocouples were installed to measure temperatures of each of the bearings and of the frame. For running-load tests, the armature thermocouples had to be removed, leaving only the resistance method for determining armature temperatures after shutdown. Resistance readings were taken by a Kelvin bridge at measured intervals of time after shutdown, and a curve of resistance versus time was plotted and extrapolated back to shutdown time; temperature was computed from this resistance.

A variation of the above method, proposed by L. W. Buell, was to determine resistance while the motor was energized by the voltmeter-ammeter method. Po-



Figure 1. Small aircraft motor with thermostat mounted on the front bracket

A cap (not shown) encloses the thermostat and entire front bracket. Rating, as an open motor, is $^1/_{15}$ horsepower, 7,500 rpm

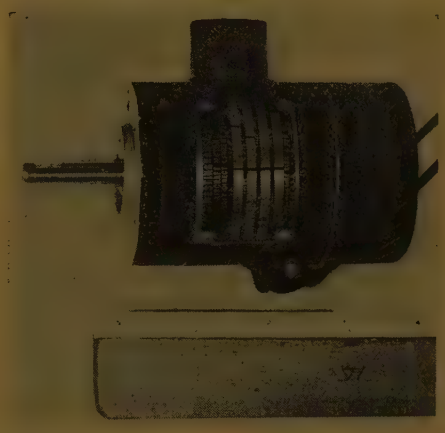


Figure 2. A $^1/_{15}$-horsepower 7,500-rpm 24-volt d-c aircraft motor with a "silk-hat" thermostat mounted on the outside of the frame

tential-drop leads were soldered to commutator risers about 90 degrees apart; voltage drop across these leads was measured and divided by the armature current, obtaining a fictitious resistance; variation of this fictitious resistance was used as an indication of temperature.

Some comparisons between temperatures by resistance and by thermocouple are of interest. Soldered thermocouples might be expected to give higher temperatures than unsoldered thermocouples, but on four locked-rotor tests, no significant and consistent difference in observed temperatures was noted. Temperatures by soldered thermocouples were compared with temperatures by resistance; of 13 locked-rotor tests, the thermocouple temperatures ranged from 2 degrees centigrade lower to 19 degrees centigrade higher, and averaged 9.5 degrees centigrade higher than the resistance temperatures. This is equivalent to saying that the temperature rise by thermocouple is approximately six per cent higher than the rise by resistance. A similar comparison was made for 56 temperature tests on fractional-horsepower motors in a previous paper; it was found that the temperature rise by unsoldered thermocouple averaged three per cent more than the temperature rise by resistances.[3]

Types of Tests Taken

Three basic types of tests on thermally protected motors are possible: locked rotor, maximum continuous load, and overload cycling. Locked-rotor tests are important because one of the major reasons for thermal protection is to prevent damage which may arise when the rotor is stalled. Maximum-continuous-load tests are taken to find out just how much load the thermally protected motor can carry without tripping the thermostat. Such a test is taken by loading the motor up to this maximum continuous load until all temperatures have stabilized, and then, increasing the load slightly, causing the thermostat to trip; immediately after tripping, all temperatures are measured. An overload-cycling test is taken by applying a load much heavier than the maximum continuous load, and allowing the motor to cycle on and off at this load value. All three basic types of tests were taken on one of the combinations listed in this paper, but the first two types were taken on all combinations.

A Typical Locked-Rotor Test

Results of one typical test showing how the thermostat cycles on and off and showing how the temperatures and motor current vary during the test are given in Table I.

At first, the thermostat cycles rapidly since the cycling time depends upon only the characteristics of the thermostat itself. In the thermostat used for these tests, the disk opens at a temperature of 147 degrees centigrade, and recloses at a temperature of 116 degrees centigrade. Therefore, during the first on period, the disk heats from 25 to 147 degrees centigrade, requiring 4.5 seconds. During first off period, the disk cools from 147 to 116, then recloses. For the second and each remaining on period, the thermostat disk has to be heated only from 116 to 147; if the motor current were

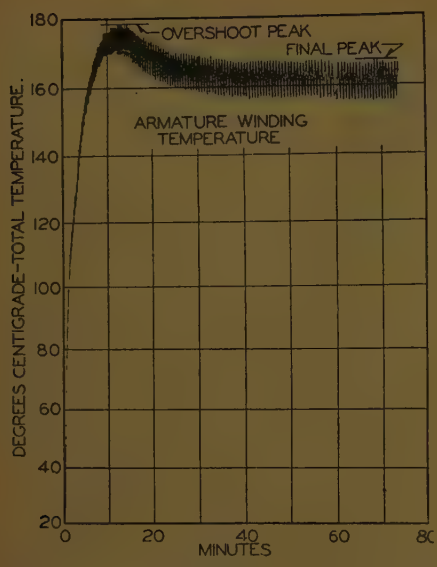


Figure 3. Temperature versus time, locked rotor

constant, this on time would be substantially the same for all remaining cycles, but as the motor current decreases because of the increase in motor temperature, the thermostat on time slowly increases, as can be noted in the table. For each reclosing cycle, the disk cools from 147 to 116, but the length of time required increases slowly as the temperature at the thermostat location increases—in this case, the front-bearing temperature.

Figure 3 shows how the temperature of the armature winding varied during the particular locked-rotor test described. This curve was drawn with a continuous one-point temperature recorder, but in the figure the time axis has been reversed so as to read from left to right. (Ordinarily, on the record of this instrument, time reads from right to left.) The first five or six temperature peaks cannot be seen on this record, since they occur too fast for the instrument to record, but subsequent peaks can be seen. If an envelope were drawn around the peak temperatures reached at the end of each thermostat cycle, this envelope would show an overshoot peak at the end of approximately 11 or 12 minutes; after this, the envelope would approach the final peak temperature asymptotically. Armature winding temperatures given in this report actually refer to the final peaks, that is, to the steady-state value of the envelope of the peak temperatures after stable conditions have been reached. From the standpoint of insulation life, the equivalent temperature actually is even less than the final peak temperature, but for simplicity the final peak temperature has been plotted in all cases.

Summary of Locked-Rotor Tests

Final results for the three motor–thermostat combinations tested are summarized and compared in Figure 4. It will be observed that there is a pronounced tendency for the armature winding to get hotter at extremely cold ambient temperatures than it does at high ambient temperatures. However, both brushholder and bearings tend to get hotter at the high ambient temperatures. In fact, there is a tendency for all the temperatures in the motor to stabilize at the same value at the high ambient temperatures. At an ambient temperature of 147 degrees centigrade (disk-opening temperature), all temperatures would be equal, because the thermostat would not allow the motor to carry any current. Of interest is the fact that similar characteristics are observed for all three motor–thermostat combinations, even including the internally mounted thermostat.

Low line voltage can often occur, and if it does, stalled-rotor conditions are more likely to occur. To determine how this condition would affect the thermal protection, a locked-rotor test was taken on the enclosed motor at 17.3 volts. Temperatures attained during this test are compared with the results of a similar test at 26 volts in the following table:

	Line Volts 26.5	17.3
Armature winding temperature:		
Overshoot peak	178	186
Final peak	176	172
Field temperature	106	109
Bearings (front)	104	102
Brushholder	105	106

No significant difference is apparent, so it is concluded that locked-rotor protection is afforded at reduced voltage, as well as at rated voltage.

It will be observed by comparing the temperatures reached with the temperature-life curves given in Figure 7 that the life of both windings and bearings under locked-rotor conditions is in excess of

Figure 4. Locked-rotor tests

⊙ Enclosed motor, thermostat on bracket
× Open motor, "silk-hat" thermostat
\+ Open motor, internal thermostat

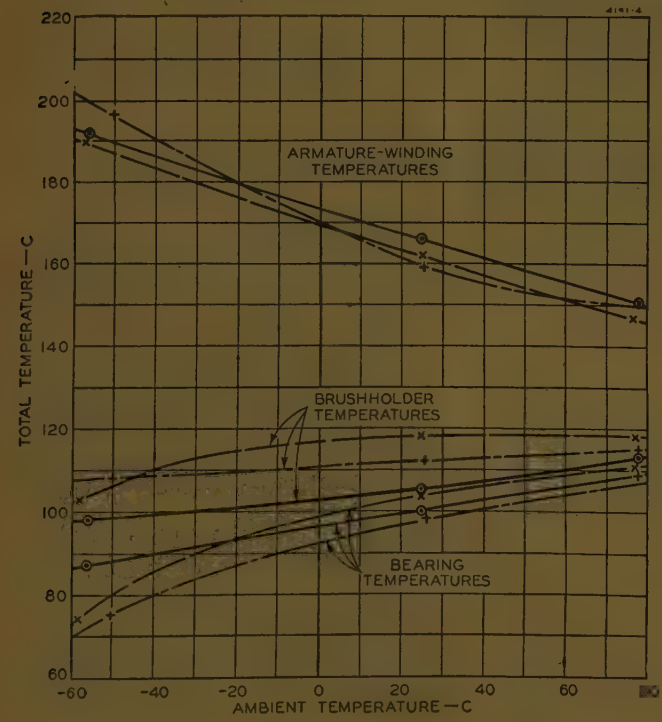
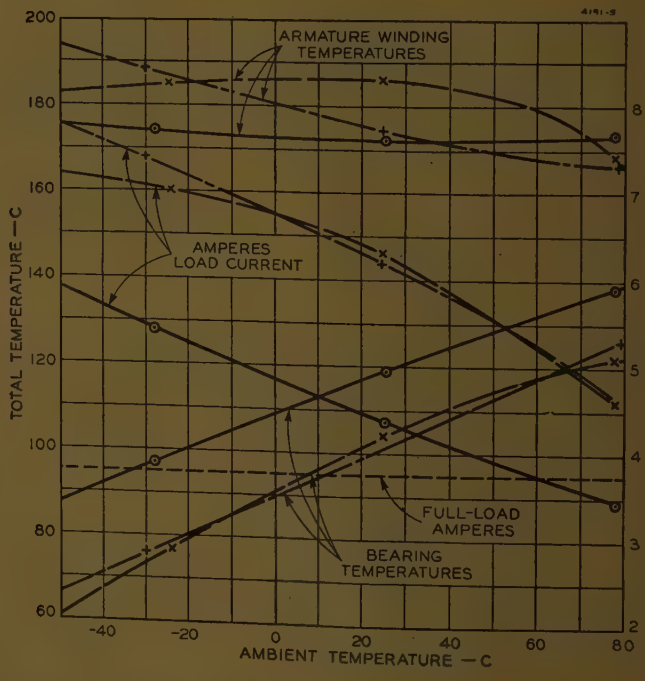


Figure 5. Maximum-continuous-load tests

⊙ Enclosed motor, thermostat on bracket
× Open motor, "silk-hat" thermostat
\+ Open motor, internal thermostat

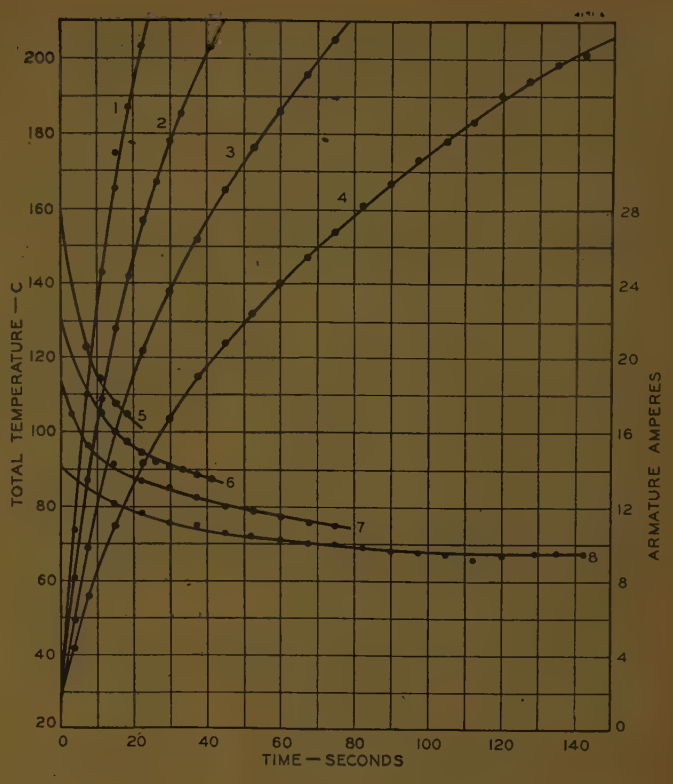


the Army's requirement of a minimum of five hours. (The corresponding life required by Underwriters' Laboratories for industrial applications is 15 days.) However, the temperatures are far in excess of those considered "safe" for industrial or domestic practice. They have been made higher deliberately, in order not to interfere with the output of the motor and because it is much more economical to operate aircraft electric equipment at high loads in spite of shortened life.

Locked-rotor heating curves for this motor were taken at four different voltages and these are plotted in Figure 6. In taking these curves the thermostat was left out of the circuit and both temperature and armature current were measured at frequent intervals. Of significant interest is the current–time curve, because torque falls off at the same rate as the current (in a series motor, it falls off faster than the current). If the load is so great the motor cannot start it cold, no useful purpose is accomplished leaving the motor on the line, for the torque steadily decreases. An unprotected motor burns out in a matter of seconds; a protected motor keeps trying intermittently for hours. If the load moves only a little with each cycle of the thermostat, the protected motor will eventually do its work while the nonprotected motor burns out and is completely useless; it is much worse than useless if it starts a fire.

Figure 6. Locked-rotor heating curves

Frame G234, 1/16-horsepower CDH aircraft motor, 24 volts, 3.75 amperes, 7,500 rpm, shunt wound, enclosed



1—Armature-winding temperature by thermocouple, at 29.9 volts
2—Armature-winding temperature by thermocouple, at 24.0 volts
3—Armature-winding temperature by thermocouple, at 19.5–19.3 volts
4—Armature-winding temperature by thermocouple, at 15.3 volts
5—Armature amperes at 29.9 volts
6—Armature amperes at 24.0 volts
7—Armature amperes at 19.5–19.3 volts
8—Armature amperes at 15.3 volts

Maximum-Continuous-Load and Overload-Cycling Tests

Maximum continuous load was defined earlier in the paper as the maximum load the motor can carry without tripping the thermostat. Experience has shown, both for industrial-type and for aircraft motors, that there is a general tendency to obtain higher winding temperatures if the motor is operated at the least possible load which will cause tripping. If the motor is operated at a load greater than the maximum continuous load, the thermostat trips in a relatively short time, but the motor temperatures are generally lower than they are at the end of a continuous run at maximum continuous load. However, if the load is maintained and if the thermostat is allowed to cycle, the final peak temperatures may be slightly higher than on a maximum-continuous-load test. Overload-cycling tests were taken only on the enclosed-motor combination, and it was found that, at or below room temperature, the armature-winding temperatures were substantially the same as for those of the maximum-continuous-load test; at higher ambient temperatures, however, there was a definite tendency to obtain lower temperatures on a cycling-overload than on a maximum-continuous-load test.

A summary of the results of maximum-continuous-load tests for all three motor-thermostat combinations is given in Figure 5. How much load the motor can carry without tripping the thermostat at various ambient temperatures is useful information, both to the motor designer and to the motor user. This is shown by the "amperes load current" curve. It is of interest that the thermostat allows the open motor to carry, even when enclosed, full-load rating up to +60 degrees centigrade. However, as an open motor, full-load rating can be carried at ambient temperatures higher than 80 degrees centigrade. It is noteworthy that the final temperatures reached by the armature winding are more or less independent of the ambient temperature, although there appears to be a slight tendency for the thermostat to cut off at lower winding temperatures when the ambient temperature is high. This trend is neither so definite nor so positive as it is in the case of locked-rotor tests. Contrariwise, it will be observed that the bearings tend to get hotter at high ambients, as might be expected; also the bearings get hotter in the enclosed motor than they do in the open motors. Limiting factor in this case is the bearing temperature of 140 degrees centigrade, which indicates a life of less than one day.

Altitude-Chamber Tests

Tests were taken in the high-altitude laboratory at Lima, Ohio, to determine what effect altitude might have upon the operation of the thermostat.[8] For tests at altitude, the pressure in the chamber was reduced, but the temperature was not, primarily for three reasons: to determine the effect of pressure alone, as the effect of ambient temperature had already been studied at ground level, some aircraft motors are operated at high ambient temperatures at altitude, and because the testing procedure was simpler and quicker. A locked-rotor test was taken, at ground level, and repeated at altitude conditions, using the same instruments, the same technique, the same personnel, and the same procedure. Similarly, a maximum-continuous-load test was taken, first at ground level, then at altitude conditions. Results of these four tests are given in Table II.

Performance of the thermostat at altitude was almost identical with operation at ground level; there was no significant difference in temperature reached, nor did the thermostat have any difficulty interrupting the locked-rotor current of 12 to 14 amperes at a density altitude of 42,000 feet.

Most interesting is the comparison of the maximum-continuous-load test at ground level with that at altitude. Armature-winding temperatures permitted by the thermostat are practically identical, and since this is the most critical

temperature, the thermostat is thus seen to be limiting the critical temperature to a fairly constant level, independent of air density. It will be observed, however, that the thermostat does not permit the motor to carry so much load at 42,000 feet as at ground level because the air density is less, and less heat can be dissipated. Temperatures of the mechanical parts are higher, however, at altitude. This point

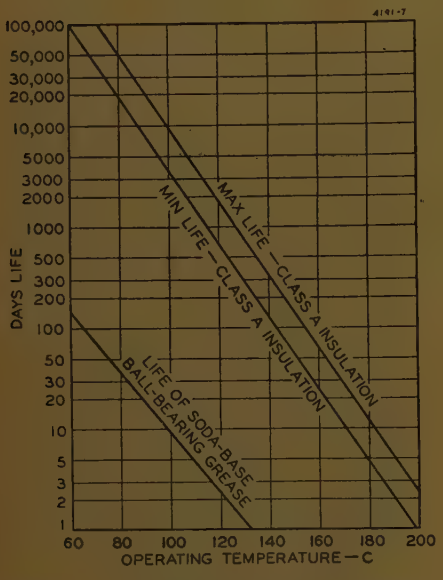


Figure 7. Effect of temperature on life of insulation and grease

Insulation life from Miner, "Insulation of Electrical Apparatus," figure 301, and grease-life information from ball-bearing manufacturers

is not difficult to understand, when it is realized that, since the total losses dissipated are less, less difference between hot-spot and frame temperatures would be expected; if hot-spot temperatures are held the same, the frame temperature will be higher with less effective ventilation. This same effect was noted for the enclosed motor in Figure 5.

Selection and Application of Thermostats

A general theory of thermal overload protection was published previously by the author.[2,3] This theory related primarily to single-phase induction motors, and showed how it was possible by means of a properly designed heater in the thermostat to make the temperature of the disk follow the temperature of the windings fairly closely. This theory was further extended quantitatively in unpublished notes to the development of mathematical formulas for selecting the right thermostat to protect any given motor properly at all loads and at all ambient temperatures. Exhaustive tests showing how effective inherent overheating protection can be in this type of motor have been reported by C. P. Potter.[4] V. G. Vaughan has outlined some of the principles involved in the selection and application of thermostats for aircraft motors.[5]

It was hoped to develop for aircraft motors a method of mathematical analysis which would select the right thermostat on the first trial the same as was done for domestic fractional-horsepower motors. However, it has not been possible so far to realize this hope for the problem of protecting aircraft motors differs from the problem of protecting domestic fractional-horsepower motors in a number of respects:

1. The principal source of heat, which is variable, is in the rotating winding of the d-c aircraft motor, whereas it is in the stator of the a-c induction motor. This means the thermostat must necessarily be removed farther in a thermal sense from the source of heat, which makes the problem fundamentally more difficult.

2. Aircraft motors must operate over a much wider range of ambient temperatures, and over a wider range of altitudes.

3. Windings must be allowed to reach much higher temperatures in order to prevent nuisance trip-outs which are many times more serious in aircraft than in domestic service.

4. Under locked-rotor conditions, the ratio of final-peak temperature to temperature at the end of the first thermostat cycle, appears to be much higher in d-c aircraft motors, than in industrial single-phase induction motors. Thus the first time of opening with locked rotor, is of less significance.

It appears that the only really satisfactory method is to determine the correct thermostat by actually running tests on a number of trial thermostats. Experience, however, is of invaluable assistance in developing simple rules-of-thumb which reduce the number of thermostats to be tried. Sometimes satisfactory results can best be obtained by making modifications in location or method of mounting the thermostat. As a specific example, when tests were first taken on the motor with the internal thermostat, it was found that considerable ventilating air was reaching the disk of the thermostat. This air tended to cool the disk, thereby permitting the motor to operate at higher temperatures on running loads; but it had no effect on locked-rotor temperatures. This difficulty was surmounted by putting a cap over the thermostat so that ventilating air could not strike the disk directly. Thus, the ratio of locked-rotor temperature to running temperature was increased without changing the thermostat itself. It is anticipated that, at other times, means may have to be taken to change this ratio of temperatures in the opposite direction. Success in applying thermostats to aircraft motors might be said to follow Edison's formula for successful invention: 2 per cent inspiration, 98 per cent perspiration.

Table II. Thermal-Overload Tests in a High-Altitude Chamber

	Ground Level	Altitude
Locked-rotor tests		
Air temp, C	+30	+33
Pressure altitude, ft		35,000
Density altitude, ft		42,000
Temp at end of test		
Armature winding by thermocouple	176.5	180.5
Shunt field by thermocouple	108.5	109.5
Brushholder by thermocouple	118	118
Bearings (front) by thermocouple	107	108
Frame by thermocouple	100.5	100.5
Thermostat case	90	93
Maximum-continuous-load tests		
Air temp, C	35	33
Pressure altitude, ft		35,000
Density altitude, ft		42,000
Maximum continuous load, amp	5.75	4.3
Temp at end of test		
Armature winding by resistance	180	186
Shunt field by thermocouple	95	126
Brushholder by thermocouple	103	130
Bearings by thermocouple	99.5	123
Frame by thermocouple	84	113
Thermostat case	80	105

Summary and Conclusions

Possibilities of inherent overheating protection for aircraft motors were investigated by taking comprehensive tests on a small aircraft motor under a wide variety of controlled conditions, including variations in: line voltage, load, ambient temperature, enclosure, thermostat location, and altitude.

Locked-rotor tests taken at ground level show that effective protection is obtained over the tested range of ambient temperatures from −58 to +78 degrees centigrade. Slightly better protection is afforded the armature winding at the upper end of this temperature range, but the bearings are better protected at the lower range, as can be seen by reference to Figure 4. A reduced-voltage test demonstrated that the protection is as effective at 17 volts as at 26 volts. Altitude-chamber tests demonstrated that the effectiveness of the protection is not impaired at 40,000 feet, also that the protective device has ample interrupting capacity at this altitude.

Running-load tests showed that effective protection is obtained over the tested range of ambient temperatures from −50 to +80 degrees centigrade. The general tendency is for the thermostat to cut the motor off the line at nearly the same armature-winding temperature, regardless of ambient temperature, although there is a tendency to limit the armature-winding temperature to a slightly lower value at the upper end of the range. However, temperatures of the bearings are invariably higher at the higher ambients, as can be seen by reference to Figure 5. When the thermally protected motor was taken from sea level

Historical Development of Electric Connectors

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Synopsis: This historical development of connectors endeavors to survey briefly the various stages of electric-connector design and the expanding applications of the uses from the earliest types for heavy-duty power applications to the smaller and more intricate fittings in aircraft, electronics, sound equipment, television, and marine applications. Because of the broad scope of the subject, the material is treated in generalizations in order to cover the history within the space limitations of this paper.

It is our belief that connector design, materials, and manufacture are keeping pace with the requirements of new electric equipment. Since the connector is essentially a component part of electric equipment, its development is affected by the requirements of new electric equipment and through adaptations for the improvement of existing equipment is increasing operating efficiencies.

THE primary role of the multicontact electric-cable connector is to disconnect and break electric circuits rapidly. It is also used as a means of switching circuits.

For wiring and servicing operations, the connector simplifies such procedure, compared, for instance, to the slow disconnection details of the screw-type terminal block. With the connector, the danger of rewiring incorrect circuits is also eliminated. The speed with which circuits can be disconnected or connected in emergencies points to another advantage as a safety factor.

In one instance, operating efficiencies of electric connectors were demonstrated in the nine hours per engine time saved in servicing and overhauling a large transport airplane.

Inasmuch as the term "plug" is often used indiscriminately to describe both receptacle and plug, it is well to define the complete connector and its two main parts. The complete connector comprises both receptacle and plug. The receptacle is that part of a complete connector which is normally "fixed," that is, rigidly attached to or an integral part of a supporting surface. The plug is that part of the complete connector which is normally removable after disengagement. Either plug or receptacle may be provided with socket or pin-contact insert assemblies. The type *AN*3101, however, is an exception to the rule.

The choice of a type letter has, for the most part, been variously adopted, depending upon the general use or even—as in the case of type *F* (Figure 1)—for the company, Fox Motion Picture Studios, for whom it was designed. Fortunately, when the Army and Navy specifications connectors were created, a standardized system of nomenclature of parts as well as types was adopted, for which the engineering profession can well be proud.

The precursor to the electric connectors as modified and improved for aircraft applications is found among those early connectors built by James H. Cannon and associates shortly after 1920. The first of these is termed the *M*1–4. It is still in use today and far from obsolescence. It was designated as type *M*, because it was designed for electric motors, 1 because it was the first of a series, and 4 for the number of contacts in the insert assembly.

Compared with the average connectors in use today in aircraft applications, Cannon type *M* (Figure 2) is oversize. Its shell, however, like aircraft connectors today, is aluminum. The inserts were fabricated of laminated phenolics, one having floating female or socket contacts with tapered bores and the mating insert having fixed split male or pin contacts which assumed the angle of the tapered bore sockets upon insertion. Even this pioneer design did not differ greatly from the aircraft connector of today.

Coupling was maintained only by the wedging action of pin and socket con-

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to 40,000 feet, the following characteristics were observed: less load-carrying ability, substantially the same armature-winding temperature, and higher temperatures of bearings. Enclosing an open motor has a similar effect.

The effectiveness of thermal protection is shown by the fact that, over the entire range of conditions tested, including the three motor-thermostat combinations, the armature-winding temperature was held between the limits of 147 and 196 degrees centigrade. Exclusive of locked-rotor tests, the armature-winding temperature was held by the thermostat, under all conditions, between the narrower limits of 166 to 186 degrees centigrade. These figures are well above the range of temperatures which are encountered in any properly applied aircraft motor under any normal conditions. Hence the thermostat will trip only when abnormal conditions are encountered. But the temperatures permitted by the thermostat are low enough to permit a life of a day or more under abnormal conditions.

In the author's opinion, the temperatures for which the thermostats were set are a reasonable compromise. Appreciably lower values might result in nuisance trip-outs, as unnecessary as they might be dangerous. Use of higher temperatures might possibly be justified on the grounds that a minimum of one day's life is unnecessary, but it should be pointed out that maximum horsepower output of a motor decreases with the temperature. Furthermore, some allowance must be made for variations in individual motors and individual thermostats. Also, there are other parts of the motor which can be damaged by excessive temperature, including: bearings, brushholders, shunts, and so forth.

Results comparable to those reported in this paper are to be expected only if skill and care are used in the application of the thermostat. But when this is done, inherent overheating protection is afforded against all abnormal operating conditions, without danger of preventing the motor from performing its normal functions. Thus motor burnouts and consequent replacement can be prevented by thermal protection.

References

1. REFRIGERATOR MOTORS NEED THERMAL PROTECTION, E. B. Bremer, C. G. Veinott. *Electric Refrigeration News*, October 11, 1933.

2. FRACTIONAL-HORSEPOWER ELECTRIC MOTORS (book), C. G. Veinott. McGraw-Hill Book Company, Inc., New York, N. Y., pages 354–61.

3. SOME PROBLEMS IN THE STANDARDIZATION OF TEMPERATURE RATINGS OF FRACTIONAL-HORSEPOWER MOTORS, C. G. Veinott. AIEE TRANSACTIONS, volume 59, 1940, pages 1055–61.

4. THE INHERENT OVERHEATING PROTECTION OF SINGLE-PHASE MOTORS, C. P. Potter. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 993–6.

5. PRINCIPLES OF AIRCRAFT ELECTRIC-MOTOR PROTECTION, V. G. Vaughen. AIEE TRANSACTIONS, volume 62, 1943, December section, pages 760–5.

6. AIRCRAFT MOTORS STILL NEED BETTER PROTECTIVE DEVICES, T. B. Holliday. *Electrical Manufacturing*, April 1944, page 94.

7. OVERLOAD PROTECTION OF ELECTRIC MOTORS, E. F. Kurtz. *Refrigeration Engineering*, May 1944, pages 366–70.

8. ALTITUDE CHAMBER TESTS OF AIRCRAFT ELECTRIC MOTORS AND GENERATORS, Fred A. Heddleson. *Aeronautical Engineering Review*, volume 3, July 1944.

tacts; yet this proved satisfactory for the applications. This is in contrast to later developments which introduced types with latch-locking devices providing a quick and positive means of coupling, threaded coupling nuts, and also coupling nuts augmented with provisions for safety wire, which presented a more satisfactory means of overcoming problems of weatherproofing and vibration.

In this early type-*M* connector, floating sockets were used. This method of mounting the socket contacts has remained a fundamental standard.

When sound motion pictures went into production in the late '20's type-*M* connectors were used for certain electric circuits, including synchronous camera motors, microphone circuits, and camera blimps, although later type-*M* connectors were exchanged for smaller and lighter fittings in many cases.

Following the type *M*1, came the *M*2, with a three-inch insert diameter having a greater number of contacts. Next came the *M*3 (Figure 2) with an insert diameter of $4^{9}/_{16}$ inches. Type M1 carried contacts for 30-ampere service, *M*2 for 30- and 60-ampere service, and *M*3 from 30- to 90-ampere service.

As can be readily seen, the trend to larger connectors carrying a greater number of circuits in the same general design reached a point of diminishing utility. For example, the *M*3-26 (Figure 3) plug weighed $3^{1}/_{2}$ pounds. Compare this to an *AN* plug with 30 similar contacts weighing 0.336 pound. Variations of the *M* fittings were made, having up to 30 circuits.

However, it was also apparent that the smaller connector would have a wider variety of uses. This opportunity came in 1925, when a certain connector failed to meet a motion-picture sound studio's requirements both in socket design and



Figure 1. Type-*F* plug, straight-type, with pin insert assembly and cable clamp

delivery. An emergency had arisen. However, within a few days, samples were submitted embodying a newly designed socket machined from one-quarter-inch-square brass rod, having a leaf-type contact spring riveted to one side. Cable clamps were cast onto the brass end bell. Pins were pressed into laminated phenolic inserts. Sockets used were the standard full-floating type, which also enabled the sound engineers to avoid variations in contact resistance when the fittings were moved during recording.

Thus the type-*F* Cannon connector was conceived and born, with *F* for Fox Motion Picture Studios as a constant reminder of its motive origin. The *F* type proved successful and soon became the standard connector, not only for cinema sound-production equipment, but in many radio stations.

Actually, it was this type *F* that caught the interest of aircraft and airline engineers, beginning the aircraft phase of connectors. But, in the meantime, to be chronologically correct, the type *P* (Figure 3)—*P* for Paramount Studios—was designed, representing the trend toward smaller and more compact connectors. Type *P* had an insert diameter of one inch, a latch-coupling device, a die-cast shell, and molded inserts. These represented definite advances in design and materials which were reflected in connectors built a number of years later. Type *O*—O for oval design—soon followed type *P* and was designed for the inductor-type microphone.

All during these years the aircraft industry was growing, if slowly, and, with the increasing circuits and new service problems, connectors were soon or later bound to be used. The opportunity

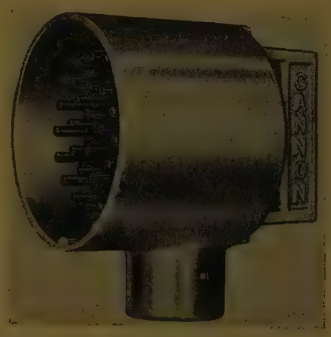


Figure 2. Type-M3 90-degree-angle plug, with pin insert assembly

came one day late in 1932, when James H. Cannon showed a type-*F* connector to Warren Boughton, chief electrical engineer at Douglas Aircraft Company. At this time the *DC*-1 transport ship was in the blueprint stage. Mr. Boughton believed the type-*F* connector to be adaptable to aircraft requirements. Modifications subsequently were made and accepted. Transcontinental and Western Airlines specified it for use on their new airliners to be built by the Douglas Company.

The shell material of the *F* was changed from brass to aluminum, and type *F* became *AF* (Figure 4), *A* for aircraft, and the transition from sound equipment to aircraft was complete.

For use on the type *DC*-2 transport airplane in 1933, the cable clamp entrance on the *AF* connector was eliminated, and threads were put on the cable entrance of the shells to permit the use of flexible conduit. At the same time, requirements for additional contacts made it necessary to increase the number of insert arrangements.

The use of connectors increased the safety of aircraft electric circuits and cut down servicing time by many hours, and maintenance of electric circuits was greatly simplified, resulting in labor and time-saving factors. Although numerous developments have been made since that time, these three basic features of electric connectors seldom have varied and remain essentially the same today.

Following closely upon the *AF*, type *FM* (Figure 5) was developed, having a two-inch insert diameter to meet new re-

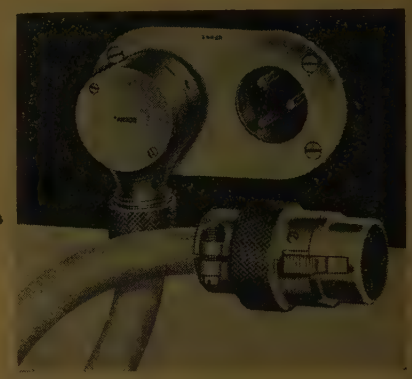


Figure 3. Type-*P* straight cord plug, with socket insert assembly, lower right. Type-*P* 90-degree-angle plug shown upper left engaged in type-*P* two-gang panel receptacle, flush-mounting, with pin insert assemblies

quirements of aircraft and the concomitant need for more contacts and greater clearance between contacts. Otherwise, the *FM* was identical to the type *F*.

The continued demand for lighter and smaller connectors used in many applications such as aircraft fire-wall connections, radio transmitters, radio control panels, tachometers, resistance thermometers, gauge indicators, remote-control thermostats, pumps, various small motors, lights, and governors, resulted in the design of the type *K*, (Figure 6), the first connector series to be designed specifically for airborne equipment. The *K* series was built in eight basic shell sizes, with inserts containing from one to 100 contacts, and in a wide variety of contact sizes, voltage spacings, and amperage ratings. Type *K* represented a marked achievement in the design of electric connectors.

In the type *K*, socket contacts were made from round rod stock, as this type of construction provided a means of grouping more contacts within a limited insert area. Thus, mechanical spacing was reduced with no loss in creepage between contacts. The insulators were fabricated out of sheet phenolic, although

during the later development of type *K* molded insulators were used in conjunction with the fabricated type. Shell parts were made of aluminum alloy. Coupling was maintained with a coupling nut having a modified Acme thread, which provided a rapid means of coupling. An exception was the large *AK* size (Figure 6) which used the conventional *NF* thread. On the *AK*, the screw-jack action of the coupling nut was necessary to engage and disengage the fitting, because of the force required to engage and disengage fittings having a large number of contacts.

The smallest type *K* has an insert diameter of five-eighths inch, and the largest, $2^1/_4$ inches. Type-*K* shell styles



Figure 4. Type-*AF* 90-degree-angle plug, with pin insert assembly, and opposite, type-*AF* wall-mounting receptacle, with socket insert assembly

were available in straight, 90-degree-angle, and wall-mounting types, including a special end bell having an integral clamp if required, and accessory fittings included dust caps, dummy, or stowage receptacles, and 90-degree-angle and straight junction shells. Cable entries in the *K* are of two types, taper and straight, with three available conduit threads in several sizes: *B* for Breeze-type thread, once standard for the United States Navy, *AC* for Air Corps and the new *AN* standard.

In 1936, a move to merge the divergent specifications of the Army Air Forces and the Navy Bureau of Aeronautics into one standard was begun for the sake of efficiency and economy. This new Army–Navy specifications on electric connectors was known as *AN*-9534. Since that time, a second specification, *AN-W-C*-591, has been drawn up, with an amended specification pending. The original *AN*-9534, issued November 1, 1939, covered 18 basic shell sizes in five different types: *AN*3100 (wall-mounting), *AN*3102 (box-mounting), *AN*3106 (straight), *AN*3108 (90-degree-angle), and type *RC* (integral-mounting). At that time 53 insert arrangements were available, whereas the total now exceeds 250.

For the type *AN*3102, see Figure 7 and Figure 8; type *AN*3106, Figure 9 and Figure 10; type *AN*3108, Figure 11.

It should be noted that type-*RC* integral mounting was intended for use where the shell of the receptacle is made an integral part of the electric equipment housing.

AN-9534 Specification:*

A. Plug and receptacle, with the exception of receptacle type *RC*, were to be interchangeable between their respective plug and receptacle shells, which applied to each manufacturer's own product and did not require the inserts made by one manufacturer to fit any other manufacturer's plug or receptacle shells.

B. Contacts were made from material of high-grade conductivity. Contacts silver-plated, except those made of thermocouple material.

C. Interior face of solder cups completely tinned.

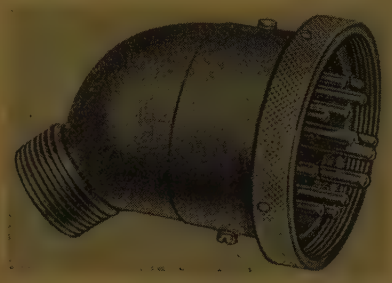


Figure 5. Type-*FM* 45-degree-angle plug, with pin insert assembly



Figure 6. Type *K* (*AK* shell size shown with *NF* thread on coupling nut)

Left, 90-degree-angle plug with socket insert assembly. Right, wall-mounting receptacle with pin insert assembly

D. Pin and socket contacts sizes 0, 4, and 8 were designed so that they could be readily removed from their inserts for soldering to conductors and easily assembled after the soldered connection was made.

E. Potential millivolt drop not to exceed 7 to 12 on contact sizes 20 to 0, with a range of current from 5 to 200 amperes, between each contact and other contacts and shell.

F. Separating force ranged from two pounds on size—20 contact to 20 pounds on number—0 contact.

G. Four service ratings—instrument (peak voltage of 70); 24 volts (peak, 100); 110 volts (peak, 350); and 500 volts (peak, 1,000).

H. Allowable creepage was based on service for which insert was designed; namely, $^1/_{16}$ inch on instrument, $^1/_8$ inch on 24 volts, $^3/_{16}$ inch on 110 volts, and $^1/_4$ inch on 500 volts.

I. Three positioning inserts made possible three distinct polarizations, numbers 1, 2, and 3, by rotating insert in shell. Thus, three identical inserts placed side by side would have distinctly different polarization.

J. Plugs were of such construction that it would be possible to install a plug on both ends of a conduit assembly without excessive slack in the conductors within the conduit. To meet these requirements, Cannon designed an end bell which was split in half so that both halves could be removed to provide ample space for servicing.

K. Shells and receptacle fittings were made of high-grade aluminum alloys.

L. Phenolic materials were used in insert assemblies.

The amended *AN*9534a specification, issued in December 1941, superseding *AN*9534, contained the following main changes:

A. Requirements for three positions of inserts were removed, and only position 1 was retained.

B. Original specifications provided for the maximum operating voltages for direct current and alternating current under the various service designations. Under the amended specification *AN*9534*a*, the a-c maximum was removed and the d-c table retained. However, reference to alternating current or direct current was removed.

C. The number of insert arrangements was increased to 87.



Figure 7. Type-*AN* 3102 receptacle with pin insert assembly

D. Type-*RC* receptacle was assigned drawing number *AND*10066.

The new specification *AN-W-C*-591, issued in December 1941, which became effective June 1942, carried the bulk of *AN*9534a as well as *AN*9534, with the following changes:

A. Deletion of markings of instrument, 24 volt, 110 volt, and 500 volt, from the face of the insulator. Instrument, 24 volt,

* Adapted from the Army–Navy specifications.

and 110 volt were grouped under service *A*, 500 volt under service *B*, and such high voltages as 14,000 under service *C*.

B. Since military aircraft, in particular, was seeking higher altitudes, effective creepage distances were increased. Service *A* with a peak voltage of 200 required one-eighth-inch minimum creepage distance; service *B* with a peak voltage of 750, $^5/_{16}$ inch; and service *C*, 14,000 volts, one inch. To maintain the interchangeability of inserts and also retain the mechanical spacing, but increase the effective creepage distance, it was necessary to design barriers on the face of the inserts.

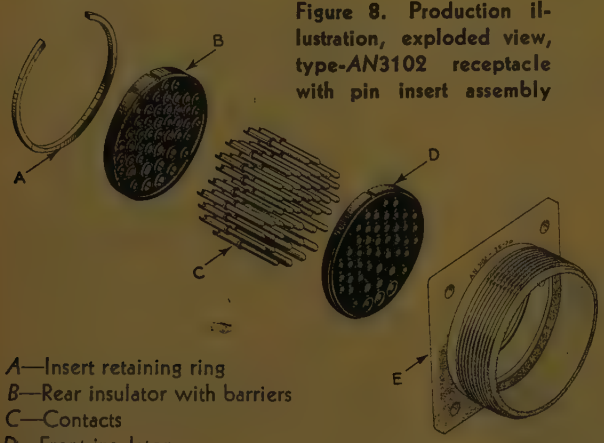


Figure 8. Production illustration, exploded view, type-*AN*3102 receptacle with pin insert assembly

A—Insert retaining ring
B—Rear insulator with barriers
C—Contacts
D—Front insulator
E—Shell

C. Shell sizes 44 and 48 were eliminated from the new specification.

D. Type-*RC* integral mounting was issued a number series for shell sizes, *AND*10425 to *AND*10436.

The newest *AN* connector is type *AN*3101 which, like the *AN*3106 and *AN*3108, embodies the portable features of a plug, but is not a fixed mounting like types *AN*3102 or *AN*3100. However, type *AN*3101 has the threaded cable entry of a receptacle. This new type is being made in sizes 8*S* to 40 inclusive, under the proposed *AN-W-C*-591*a* specification. Another new *AN* type under this specification is the *AN*3017, quick-disconnect fitting, which is restricted to sizes 8*S* to 20.

Among other connectors which are used in one way or another on aircraft or other war material which are of considerable importance but rather specialized in application, are the type *DP* and battery connectors, both developed within the last four or five years.

The *DP* is best generally described as rectangular in shape. *DP*'s are particularly adapted to rack-mounted radio and range-finding equipment. Several *DP* types mount directly to the outside face of the chassis itself, alignment being effected by a taper in the shell and by the contact arrangement itself. Coaxial contacts for radio antennas are standard in several of the *DP* fittings. Contacts vary from 10 to 135, the largest connector in the series being type *DPL* used for special equipment by the Army Air Corps.

A notable development of the line of



Figure 9. Type-*AN*-3106 plug, with socket insert assembly

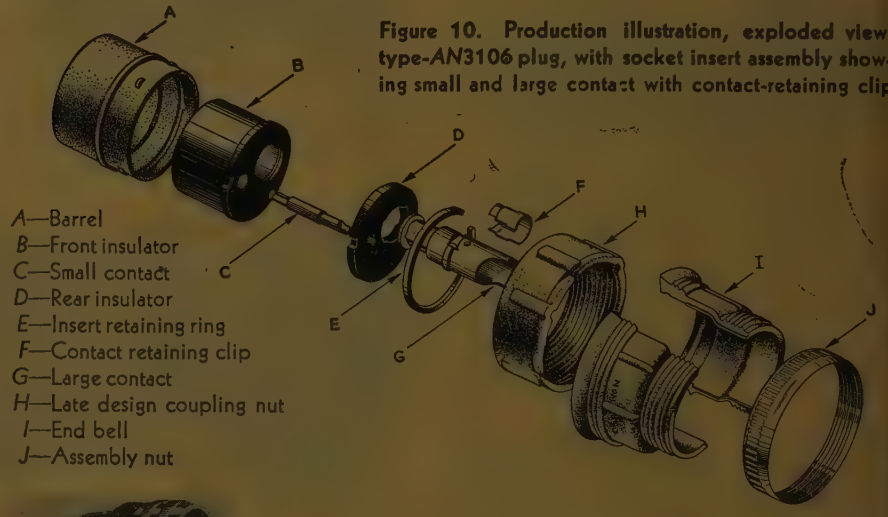


Figure 10. Production illustration, exploded view, type-*AN*3106 plug, with socket insert assembly showing small and large contact with contact-retaining clip

A—Barrel
B—Front insulator
C—Small contact
D—Rear insulator
E—Insert retaining ring
F—Contact retaining clip
G—Large contact
H—Late design coupling nut
I—End bell
J—Assembly nut



Figure 11. Type-*AN*3108 90-degree-angle plug with socket insert assembly

battery connectors, which have been in use for a number of years in engine starting and other applications, is the type numbered 11,749 and 11,751, which was developed at the request of the Air Corps to facilitate disconnection of batteries, particularly in extremely cold climates. The design formerly in use required the maintenance mechanic to loosen two small wing nuts before removing the battery, a slow and difficult process in subzero weather. During removal, the battery contacts were likely to strike the aluminum side of the air frame, creating a short circuit and consequent fire hazard, whereas this new development completely encloses the contacts. In addition, it has a handwheel for disconnection, which can easily be turned with a gloved hand.

The materials in the insert insulators constantly are being improved to overcome high-altitude problems, humidity, and fungus growths of tropical climates, and circumstances of salt-water conditions. Research in thermoplastics, thermosetting materials, and protective coatings is going forward rapidly.

Briefly, among the other developments which are yet to be recorded in the chapters on the history of electric connectors are snap-in solderless contacts. Still other new developments not entirely out of the experimental stages include the following: connectors having a greater voltage for high cycle; special switching devices which operate engine-starting units; and waterproof designs for marine and ordnance requirements.

The Testing of Brushes for Life and Performance Under Various Altitude Conditions

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Synopsis: This paper presents the author's experience in the testing of brushes on machines intended for high-altitude service on aircraft equipment. The procedure and range of conditions found useful in testing a wide variety of small a-c and d-c machines are outlined in detail. It is hoped that other investigators will contribute their experiences through discussions or additional papers, and that from this material there may be developed a suitable test code, supplementing the present Institute test code for d-c machines.

The only real criterion of brush performance is that of actual experience in service, since all of the infinite variety of conditions that affect brush performance cannot be anticipated under test conditions. However, carefully conducted tests over a range of specified conditions do provide a good basis for brush selection and greatly expedite the final choice for the best over-all performance. For this reason, and because it will be helpful to investigators in comparing their results, it is hoped that the proposed brush test code may be undertaken in the near future.

IN brush-life testing on machines, many confusing, conflicting, and variable results are observed even under normal atmospheric conditions. These uncertainties are increased greatly in high-altitude tests. Many tests by various laboratories have shown that most brushes are affected greatly by the changes in humidity, oxygen content, or pressure of the surrounding air. It is a common experience for carbon brushes to wear away at extreme rates up to half an inch or more per hour when run on high-speed commutators or slip rings in extremely dry atmospheres.

This rapid wear can be inhibited by a great variety of contaminants if present in small quantities in the air, in the brush, or even in the metal of the commutator. Test results therefore are likely to vary greatly unless scrupulous care is taken to exclude unwanted materials and to maintain precise control of the atmosphere.

It is not the purpose of this paper to discuss any of the methods for preventing brush wear, but simply to describe the testing procedure found useful in obtaining definite and reasonably consistent results in the measuring of brush performance. Uniform methods of conducting and reporting brush-life tests are proposed, so that wherever or by whomever tests are made, the results will be of value for record and will be truly comparable with other tests made at other times and places.

General Considerations

The primary purpose in brush testing is to determine, as nearly as possible, if satisfactory operation will be obtained in actual service. Therefore, all known conditions of loading, atmosphere vibration, and power-supply characteristics which occur in service should be duplicated as closely as is practical within the limitations of testing equipment. On many aircraft applications the ranges of load and atmospheric conditions are so extreme and duty cycles are so variable, that the establishment of adequate test cycles may require very careful study and judgment. In many cases it may be impossible to establish satisfactorily brush performance until the representative loads encountered in service are known.

Nearly all of the electrical and many of the mechanical defects which commonly occur on machines adversely affect the life and performance of the brushes. It is impossible to evaluate the brushes unless adequate measurements are taken to make sure that electrical and mechanical conditions of the machine remain substantially constant throughout the test. In order to differentiate between brush failures due to properties of the brush material and the failures induced by mechanical and electrical defects occurring in particular machines, it is necessary to make very comprehensive measurements and observations of all factors known to affect brushes.

Tests should be taken only on machines in good working condition, because the effects of many electrical and mechanical defects upon brush operation seldom can be appraised accurately. Small machines often are damaged by rough handling causing brinelled ball bearings, bent shafts, damaged or dirty commutator or collector surfaces, bad balance, bent or improperly located brush holders; all are factors which adversely affect the brushes. Where there is any possibility that careless handling has occurred, very careful mechanical inspection should be made before any tests are begun.

To predict brush life reasonably well on the basis of relatively short tests, careful study of the nature of commutator, ring, and brush wear is necessary. It appears necessary also to study the films formed on commutators and rings for evidence of any marked changes in appearance with time. The gradual formation of excessive or uneven film during several days of operation has often appeared to be the principal cause of brush failure.

The "Brush-Qualification Test Procedures" outlined in the appendix have as their purpose the determination of three fundamental factors of brush operation which are often almost equally important. These factors are: brush life, commutator life, constancy and suita-

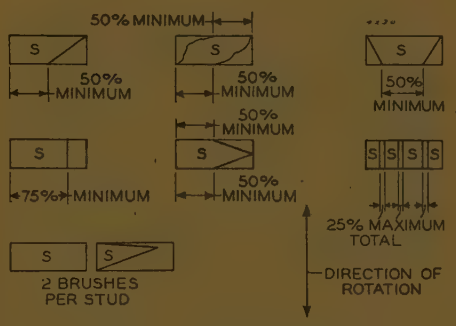


Figure 1. Views of brush face

S represents seated portion of brush

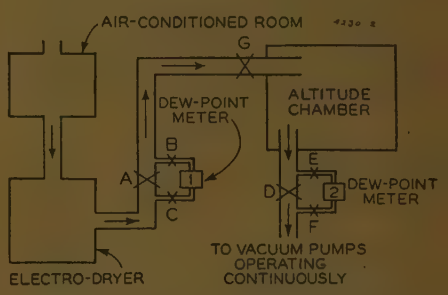


Figure 2. Schematic diagram of a dependable high-altitude chamber system

Valves *A*, *B*, *C*, and *D*, *E*, *F* are used only to produce sufficient pressure head across the dew-point meters to produce distinct meter readings. Meter 1 reads dew point of the air entering the altitude chamber. Meter 2 reads dew point of the air usually in center of altitude chamber. Throttling valve *G* is adjusted to maintain the required altitude

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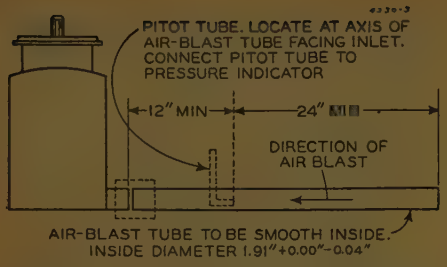


Figure 3. Typical Pitot and air-blast-tube installation for test of aircraft-engine generators

bility of machine characteristics as affected by the brushes. The use of any brush grade usually represents some compromise among these three factors. To match the brush with the machine to secure required performance characteristics is often more important than to match brush and machine to secure maximum brush life.

Types of Tests

It has been found useful to classify tests according to two general types of atmospheric conditions which are generally designated as low altitude or high altitude.

Low altitude covers the ranges of atmospheric conditions usually encountered in test departments and laboratories. The air should be relatively free from chemical, smoke, paint, and any other fumes which are known markedly to affect brush operation. It is proposed that low altitude should include the following listed range of conditions:

1. Altitude from sea level to 1,000 meters.
2. Ambient air temperature from 20 to 40 degrees centigrade.
3. Relative humidity from 20 to 90 per cent.

High altitude is defined as being any atmospheric condition above 1,000 meters in test chambers where the air pressure, dew point, ambient temperature, and air cleanliness are controlled to simulate known high-altitude atmospheric conditions. Generally our experience and the reported results of other investigators have shown that there are no marked changes in brush performance until free-air altitudes of 10,000 to 15,000 feet are exceeded. Because of these experiences and the practical need to limit variations in test conditions to a reasonable minimum, it is proposed that only four general classes of high altitude be adopted as standard. These classes are based upon the estimated atmospheric conditions encountered in or on airplanes, and are proposed to be as shown in Table I.

Classes I, II, and III of Table I are intended to apply to all totally enclosed or self-ventilated machines. Class IV may apply to either self-ventilated or rammed-air-ventilated machines located in the engine nacelles.

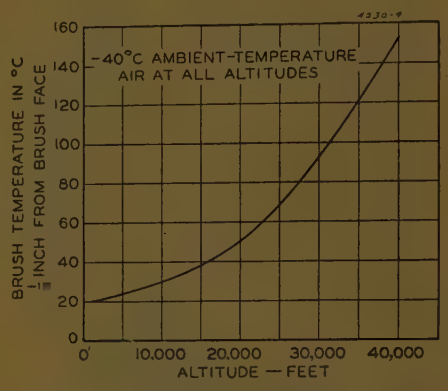


Figure 4. Brush temperature versus altitude

Many machines without adequate ventilation for cooling brushes and commutator at high altitudes soon reach critical temperatures as altitude is increased

Conclusions

The test procedures listed in the appendix have been worked out as a result of conducting some 600 tests on small a-c and d-c machines for aircraft use over a range of from six watts to four horsepower, and from 2,500 to 15,000 rpm. When these methods are followed, it is believed that brush life will be consistent within the range of perhaps two to one on duplicate machine.

It is very important, however, to note all differences and conditions which may have a bearing on brush performance, so that if later tests give different results, the reason for the difference may be traced.

In studies of brush application, the individuality of different machine designs makes over-all solutions improbable. However, it is believed that close agreement can be obtained by different observers at different times and places on the basic question, "What is a good brush for the particular application?" if the "Brush-Qualification Test Procedure" listed in the appendix is followed. It is proposed that these test procedures and "Types of Tests" should be combined suitably to form a brush test code.

The testing of brushes for life and performance involves two distinct types of measurements. These are the reasonably exact mechanical, electrical, and thermal measurements; and the relatively inexact observational measurements for which the accuracy and usefulness largely are dependent upon the observer's experience in studying brush phenomena. It does not seem likely that sufficient exact measurements can ever be made to provide the complete answer regarding the over-all suitability of the brush for the application. However, meticulous attention to test procedures reduces the usual degree of dependence upon inexact observational measurements, reduces the hazards of possible wrong decisions in the selection of brushes, and greatly improves the usefulness of observational measurements because of the sound background of other factors on which they are based.

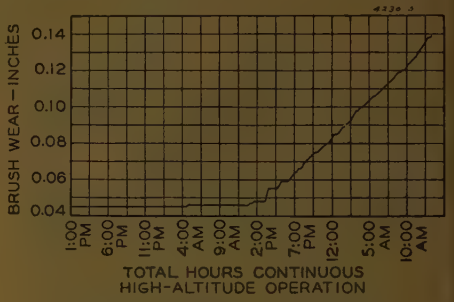


Figure 5. Brush wear versus time

Quick changes to a new greatly increased rate of wear often are encountered at high altitude

Table I

Atmospheric Conditions	Class I Free Air External	Class II Fuselage—Air Within the Airplane	Class III Supercharged Compartment	Class IV Engine Nacelle
Altitude in feet	40,000	40,000	15,000	40,000
Dew point in degrees centigrade	−50 to −80	−50 to −80	−50 to −80	−50 to −80
Ambient-temperature air in degrees centigrade				
(*a*). Surrounding the machine	−40	0	0	+38
(*b*). Rammed air 6 inches from machine intake ports				−30*
Rammed-air pressure (Sum of velocity and pressure head measured with Pitot tube at least 6 in. away from machine intake ports. See Figure 2)				6-in. water*

* Conditions do not apply to self-ventilated machines.

Appendix. Brush-Qualification Test Procedure

The following specific rules have been found desirable in conducting brush tests:

1. Initial Inspection

Carefully check the machine to make sure it has not been damaged mechanically in any important particular. Make sure that balance is satisfactory as evidenced by ac-

ceptable vibration, that end-play adjustment is correct, that brushes are free in brush holders with springs in proper place and with brush pressure correctly adjusted, and that commutator or ring run-outs on bearings meet standard limits for the equipment.

2. Preparation for Test

On freshly ground, turned, or polished commutators or rings, operate units approximately five hours at light loads to obtain a satisfactory brush seat and to establish approximately normal brush film on commutators, rings, or other current collecting surfaces.

Many machines will not give satisfactory performance characteristics until the brushes are seated properly. It generally is desired to have brushes seated 100 per cent before beginning tests, but where this is not practical due to limited time, brush seats equal to or better than those shown in Figure 1 have been found satisfactory. All brushes must be seated 75 per cent of face area and 100 per cent of brush thickness for 50 per cent of the brush width. Where there are two or more brushes per stud, all brushes on one stud are to be considered one brush so far as brush seat is concerned.

3. Measurements and Methods

The measurements and observations listed in sections *a*, *b*, *c*, and *d* generally are taken at the beginning of the brush test, at the conclusion of the normal-altitude runs, and again at the conclusion of the high-altitude runs. During continuous-duty test runs, the test readings listed in sections *e*, *f*, *g*, and *h* generally are taken every ten minutes until the machine and associated test apparatus reach reasonably steady operating conditions. Then the intervals may be lengthened up to one hour or more. To insure detection of all significant changes, recording instruments should be used wherever possible.

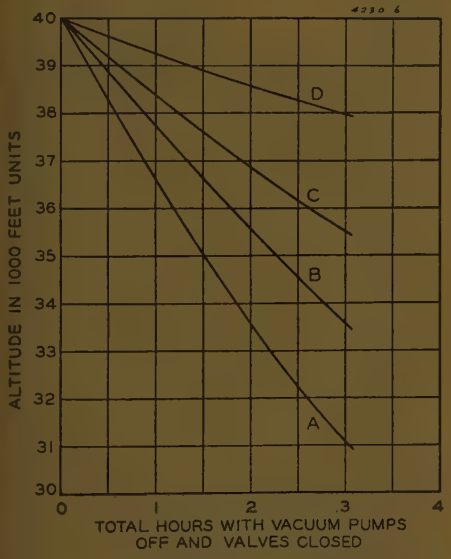


Figure 6. Altitude-chamber leakage control

A and *B* = worst and best conditions over one month interval = adequate tightness to obtain —50-degree-centigrade dew point

C = best obtainable after leaks around screws through the walls were sealed

D = a second chamber with an improved door seal





Figure 7 (left). Brush-wear indicating device

Figure 8 (above). Test machine with brush-wear indicator in altitude chamber

a. Commutators. Measure diameter, total gauge, and bar-to-bar run-out in the brush track. Where experience indicates there is any possibility of bar movement, commutator run-outs also should be checked outside the brush track. Note any irregularity across the face of the individual bars, such as "crowning," indicating faulty machining.

Inspect undercuts for any evidence of feather-edge mica, excessive burrs, foreign material.

b. Slip Rings. Check diameters, gauge run-outs, and note any evidences of faulty machining, such as wavy surface.

c. Brush Lengths. Due to the wide variety of shapes, no standard method of designating brush length can be used. Generally measure lengths from the same locations shown on the brush drawing for the particular machine. Measuring to the edges of brushes is often necessary and is satisfactory if micrometers are used very carefully to prevent crushing of the brush edges.

Brushes should be numbered by scriber or other means on a suitable surface for test identification and for location identification for further measurements. Brush wear is often only a few thousandths of an inch during the practical test time used. Therefore, great care in taking measurements is necessary if large percentage errors are to be avoided.

Wherever practical, the use of a continuous-indicating brush-wear device is recommended. The device should have extremely small inertia and have no measurable affect upon brush pressure. A dial-type indicator which has these characteristics, is free from backlash, and is easy to attach to the brush is pictured in Figure 7. A typical installation of the wear gauge in use on a test unit in an altitude chamber is shown in Figure 8.

d. Brush-Spring Pressures. Measure with brush in the location corresponding to its riding position on the commutator or ring. With many mechanisms the pressure may be determined easily by measuring the pull necessary to lift the spring off the top of the brush. On other types where the spring is maintained in position on the brush and the pressure is measured at the brush face, the brush mechanism should be vibrated during these measurements to eliminate the affects of friction in holders. Note any brushes which have sluggish or sticky action in the brushholders.

e. Machine Temperatures. These are taken on all parts where temperature affects the life and reliability of that part, or of the brushes. In general, the minimum points of measurement are as follows:

(1). Two motor brushes (one of each polarity). On small brushes with face area less than one-tenth square inch, the use of five-mil wire thermocouples is recommended. Whatever the brush size may be, it is important that the thermocouples have sufficient flexibility and that they are carefully installed to prevent any interference with brush movement. To permit brush wear during tests, thermocouples usually are installed one-fourth to three-eighths inch away from the brush face. Figure 4 indicates the need for measuring brush temperatures whenever possible.

The brush face temperature is the basic measurement needed. Therefore, thermocouples should be installed as close to the brush face as rate-of-wear conditions during the practical test time will permit. The temperature gradient between brush face and thermocouple location will vary greatly among different brush materials and with different ventilations. The gradient on nonmetallic brushes will be relatively high.

To avoid erroneous thermocouple readings due to stray currents, it is desirable to keep thermocouples electrically insulated from the brushes and the machine. Wire insulating varnishes have been used to insulate the thermocouple tips in a manner that causes very little temperature gradient between the thermocouple and the brush.

(2). Each slip-ring brush.

(3). Each bearing housing as close to the bearing as possible.

(4). Motor case.

(5). Field windings or other stationary windings.

f. Altitude-Chamber Ambient Temperatures. Ventilation conditions in chambers are often far from ideal, because the chambers are relatively small; temperature gradients, air turbulences and flows may exist in connection with the refrigerating systems; and large gradients may exist between the chamber wall and case temperatures of the machine under test.

Check temperature on the chamber walls, in air six inches from the test machine intake air ports, and at as many other points as necessary to determine average air ambient temperature six inches from the test unit. In rammed-air-cooled machines the temperature should be taken in the center of the air duct six inches from the surface of attachment of air duct to the machine. Keep ambient-temperature thermometers appreciably removed from the exhaust air of the test unit.

g. Machine Performance. Take measurement of: input volts, amperes, and watts; speed; output volts, amperes, and watts; output or input torque; field amperes; and any other readings required to determine completely electrical performance of the machine.

h. Altitude. Use altimeter or barometer.

i. Dew Point. Measure at beginning of test and at least every five hours thereafter. The measurement desired is the dew-point temperature of air entering test machine. Accurate values sometimes are difficult to obtain, particularly in altitude chambers. When testing self-ventilated machines in chambers having appreciable leakage, it is important to measure dew point of air near intake of test unit. A method for this is shown in Figure 2.

With negligible leakage, it would be adequate to measure dew point in only the dry-air supply lines to the chamber. Typical leakage-control tests are illustrated in Figure 6. Experience indicates that leakage control to assure low dew point is the most critical problem in high-altitude chamber operation. One cubic foot of leakage air may carry as much moisture into chamber as several thousand cubic feet of air from an alumina dryer.

CARBON BRUSH TEST REPORT

Tested at _____ Works in Bldg. _____ Machine Model _____

Serial Number of Test				
Machine Serial Number				
*Ring or *Commutator Metal				
Where Was Metal Obtained?				
Brush Grade and Treatment				
Where Were Brushes Obtained?				
Atmosphere (air, nitrogen, etc.)				
Refrigerant (dry ice, Freon, etc.)				
Dew Point (°F or °C)				
Altitude—Feet				
Machine Speed in RPM				
Load Amperes				
Brush Density—Amps/Sq In.				
Field Current—Amps				
Ambient Temp (°F or °C)				
"Air In" Temp (°F or °C)				
Brush Temp (°F or °C) Max.-Min.				
Brush Temp (°F or °C) Average				
Ring or Comm. Temp (°F or °C)				
Spring Force—Oz Ave. Initial				
Spring Force—Oz Ave. Final				
Friction Watts Max.-Min.				
Friction Watts Average				
Brush Contact Drop Max.-Min.				
Brush Contact Drop Average				
Hours Tested—Total				
Hours Tested at Altitude				
Actual Brush Wear—Inches				
Indicated Brush Life per ¼"—Hours				
Ring or Comm. Wear (Diam) Inches				
Date of Test				

REMARKS: Commutation, Film Condition, Vibration, Ventilation, Reason for Stopping Test, etc.

*Indicate which Date Signed

Figure 9. Brush-test report

4. General Factors Indicating Brush Suitability, Troubles, and Probable Causes

Observations of the following will assist in judging brushes and diagnosing trouble.

a. Surface Condition of Commutator or Slip Rings

(1). Bar-to-bar burning, uniform or nonuniform.

(2). Wearing out-of-round.

(3). Ring grooving or threading, severity and stability.

(4). Skip burning. Note any evidence which indicates the cause (bad mechanical run-outs, faulty brush mechanisms, bad rotor balance, defective bearings, brush overfilming).

(5). Uneven film.

(6). Indications of polarity spots when collecting alternating current on slip rings.

b. Uniformity of Brush Wear. The following possibilities should be investigated when wear of individual brushes deviates more than plus or minus 20 per cent from average:

(1). Polarity effects of brush material.

(2). Uneven spacing of holders.

(3). Uneven brush-spring pressures.

(4). Brushes stuck in holders or binding of pivoted holders.

(5). Brushes incorrectly positioned (wrong electrical shift).

c. Brush and Commutator Temperatures. Consider effects upon life of bearings and the insulation on brush holders, brush shunts, armature winding, and connections to commutator.

d. Mechanical Strength of Brushes. Are the brushes suitable for:

(1). Type of holder? Consider brush sidewear.

(2). Service vibration conditions? Particularly consider the shunt connections.

e. Effects of Brushes on Machine Characteristics. Check the over-all performance with respect to:

(1). Power input over the load range.

(2). Contact drop where machine characteristics (in addition to commutation) are affected greatly by it, as on dynamotors, speed-regulated motors, voltage-regulator circuits, and self-excited generators. Does the contact drop change sufficiently to disturb voltage regulation or to prevent voltage build-up of self-excited generators? During long test runs make periodic checks to determine any marked changes in contact drop.

(3). Commutating ability for expected manufacturing variations of a particular class of machines.

(4). Consistency of performance:
From zero to high current densities.
From low to high humidity.
From lowest to highest operating temperature.

(5). Brush riding characteristics, as indicated by ripple voltage, radio-interference filter requirements, and amount of brush chatter over operating range.

5. General Precautions for High-Altitude Tests

a. Altitude. Maintain within plus or minus 1,000 feet.

b. Ambient Temperature. Maintain within plus or minus five degrees centigrade.

c. Contamination. Admit fresh air into the altitude chamber continuously to avoid accumulation of contaminating vapors.

The vapors from bearing greases, incompletely cured paints, and insulating varnishes of new machines have greatly improved the high-altitude brush wear in specific chamber tests, until the most volatile constituents evaporated and insufficient contamination remained to lubricate the brushes. On totally enclosed or only slightly ventilated machines, this self-contamination may be pronounced, and there is often a great difference in brush performance on new and old machines.

When practical, recirculation should be avoided and only fresh air admitted to the test machine. This can be done readily with pressure-ventilated machines, but is not feasible for most self-ventilated units because of interference with ventilation and radiation. Experience with recirculating ventilated chambers indicates that contamination usually becomes negligible if the chamber air is changed five to ten times per hour. The recommended frequency of changes depends on the relative size of chamber and test units.

A chamber with recirculation should not have thermal insulation, braid-covered leads, varnish-cloth tape, wooden platforms, or other absorbent materials inside. It should be cleaned thoroughly before every test. Steam cleaning followed by a bake to dry out all moisture has been found satisfactory. The chamber should be flushed with dry air before refrigerating. The walls and cooling coils, if any, should not accumulate objectionable frost during an altitude test.

d. Need for Long Tests. On high-altitude trials of less than one hour, indicated brush life may be many times that obtained on a continuous-run-to-destruction. Some altitude-protected brushes may operate well for ten hours or more, and then suddenly dust, overheat, become unstable, cause excessive friction, or high-contact drop. It is important to take relatively long trials as may be noted in Figure 4.

e. Initial Equilibrium Conditions. High-altitude conditions usually are established before starting the test machines, thereby providing a more severe test for the brushes. Climb and dive cycles sometimes must be simulated, but unless otherwise specified, equilibrium conditions should be established first.

6. Duration, Repetition, and Sequence of Tests

Tests should be taken at rated speed, voltage, load (or load cycle), or at defined specification conditions.

a. Duration of Low-Altitude Tests. A qualification life test should be a minimum of 300 hours, or 25 per cent of guaranteed life, whichever is shorter.

b. Duration of High-Altitude Tests. A qualification life test should be a minimum of 30 hours, or 25 per cent of guaranteed life, whichever is shorter. If necessary, the test may be divided into minimum runs of 15 hours or more with intervening low-altitude trials.

c. Repetition of Tests. Because of variations in machine and test conditions, the results with three or more different units should be averaged to obtain representative brush life for a specific machine design. When only a single machine is used for tests, the results from two or more life tests should be averaged to obtain representative brush life for the particular machine.

d. Sequence of Tests. Low-altitude tests usually are taken prior to high-altitude tests because:

(1). The film formed at low altitude may influence the high-altitude performance. Most units operate initially at low altitudes in actual service.

(2). Some brushes lose their high-altitude protection during extended runs. This characteristic might not be revealed by a short-altitude test only.

(3). Mechanical deterioration of machines adversely effects brush performance. Chamber tests should simulate such unfavorable conditions as are likely to exist in flight.

(4). The more volatile components of lubricants, insulating varnish, and motor paint tend to be driven off during extended low-altitude runs. This reduces contamination of the altitude chamber.

7. Methods of Reporting Test Results

To assist in reporting all significant facts, a standard test data form similar to Figure 9 is recommended.

Solderless Terminals

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In the last few years solderless terminals have found an increasingly wide application. The fields of application include aircraft, shipboard, communication, and general industrial work. One of the most important reasons for their increasing use is the substantially decreased labor required in their application, as compared to soldered terminals. The use of solderless terminals also gives assurance of a greater degree of uniformity with respect to both mechanical and electrical properties for the terminal conditions of the electric circuit. Furthermore, these terminals are more readily adaptable for such improvements as insulation supports for the wire insulation to which the solderless terminal is crimped and the use of a preinsulated terminal. In general, the use of solderless terminals in both manufacture and application permits machine production and uniformity as compared to handwork necessary for soldered terminals.

Description

A type of solderless terminal is shown in Figures 6 and 10. These terminals consist of a tongue or lug for terminal-block connection, and an internally serrated barrel with sleeve for crimping to the wire. In general, the stranded wire with insulation stripped off the end is inserted for the full length of the barrel, and then two adjacent lateral crimps are applied. This crimping may be accomplished by either a hand tool or a machine press. An enlarged picture of this terminal through one of the crimp sections for a 19-strand wire is shown in Figure 1. The section has been etched with a solution of ammonium hydroxide and concentrated hydrogen peroxide to indicate clearly the individual strands of wire following crimping. Sufficient pressure is exerted in the crimping operation to give a flow of metal, so that maximum contact area between the strands and between strands and terminal barrel is obtained. This gives maximum electrical conductivity for a given volume of metal. Furthermore, this would give maximum tensile strength for the terminal, and also would give a minimum number of longitudinal channels for entrance of corrosive liquids or condensates. In general, the crimp is designed to give a tensile strength of the wire to the crimped joint approximately equal to that of the stranded wire itself. Comparative tensile-strength test results are shown in Figure 2. In these tests failure was obtained by breaking of the stranded wire itself rather than by the stranded wire pulling out of the crimped joint. The differences between the tensile strengths of the stranded wire itself and of the stranded wire when crimped to a terminal is due to different methods of terminating the wire in the two cases, with better equalization of individual strand stress being possible in the first case.

Soldered Terminals

Since soldered terminals have been used for a long time, it is perhaps natural that any other method of adapting a terminal to a wire would be analyzed in comparison with the performance of a soldered terminal. Certain of the inherent difficulties in obtaining a satisfactory soldered joint are not generally appreciated. For electric conductors

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tin–lead solder is generally used with either copper terminals or tinned-copper terminals and wire. A properly soldered joint is dependent, however, on the simultaneous achievement of cleanliness of metal surfaces and sufficient heating of the parts to be soldered. A successfully soldered joint involves the actual alloying of the soldered parts with the solder itself. Insufficient heat will cause the solder only to wet the metal, but not to alloy with it. Since there is no difference in the appearance of the two types of soldered joints, it is extremely difficult to determine whether two metals are soldered properly. The presence of large beads of solder on a joint does not mean necessarily that the metals have been soldered properly. In fact, unnecessary solder prolongs the cooling time and enhances the possibility of movement of the parts to be joined, causing the solder to crystallize, thereby weakening the joint.

In general, the electrical conductivity stability of a properly soldered joint is practically constant under long-time corrosion conditions. However, improperly soldered joints have shown relatively large increases in resistance under corrosion conditions. Such increases may be of the order of a few hundred per cent or more, depending upon the degree of departure from ideal soldering conditions.

Test Procedures and Measurements

One of the most important electrical characteristics of a terminal or electric connection is the stability of the electrical conductivity. This may be determined by an electrical-resistance measurement. The initial resistance is of importance, since it is a measure of the electrical efficiency of the contact as a conducting medium. The change in resistance under corrosion conditions is of importance, since it will determine the electrical stability of the contact under operating conditions. This stability under operating conditions is particularly important, since any great increase in resistance generally gives increased operating temperatures and a consequent increase in the corrosion rate.

Since corrosion plays a large part in determining the relative electrical stability of a solderless terminal, the design and metallic composition of a terminal must of necessity preclude any extensive corrosion. The terminal must be able to withstand operating conditions in which it may be exposed to high humidities, natural waters, chemical salts and brines, and high temperatures. The terminal must be crimped in such a manner that no corrosion occurs because of cracking or fatigue in the metal. The metal used in the construction of the terminal must be such as to obviate any electrochemical corrosion due to galvanic action between the terminal and the wire.



Figure 1. Enlarged picture of crimped solderless terminal

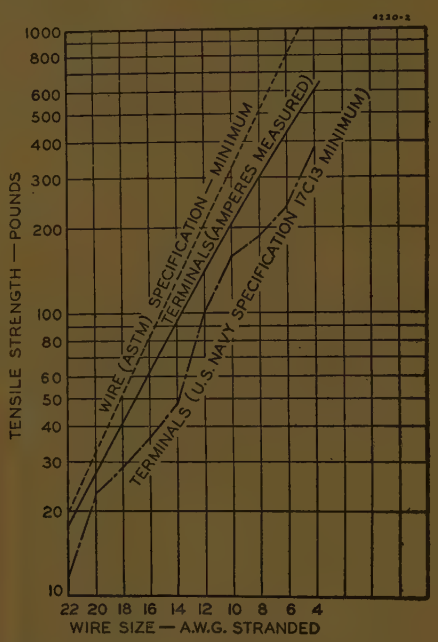


Figure 2. Tensile strengths—terminals and wire

Electrical measurements of resistance of solderless terminals of the type described in this paper have been made under the following types of corrosion conditions:

1. Continuous four per cent sodium-chloride spray.

2. Continuous ten per cent sodium-chloride spray.

3. Continuous four per cent sodium-chloride with 0.1 per cent sodium-hydroxide spray.

4. Alternating four per cent sodium-chloride spray and circulating-air oven at 100, 133, 166, and 200 degrees centigrade.

5. Alternating four per cent sodium-chloride and 0.1 per cent sodium-hydroxide spray with 100 degrees centigrade circulating-air oven.

6. Relative humidity of 85 per cent alternating with 100 degrees centigrade circulating-air oven.

7. Alternation of 100 degrees centigrade circulating-air oven, four per cent sodium-chloride spray, and vibration test at $^1/_{32}$-inch amplitude at a frequency of 2,000 cycles per second—vibration test made to give flexing of stranded wire relative to terminal.

8. Sulphur dioxide vapor.

9. Alternating and direct currents corresponding to approximately three times normal rated currents for periods of three-fourths hour, alternating with different salt-spray tests.



Figure 3. Assembly of 1,000 solderless terminals for electrical measurements during corrosion tests

The foregoing corrosion tests have been made on different terminals for varying periods of time. In certain cases they have been continued for a period of 150 days. It is believed that such corrosion conditions are very much more severe than those normally encountered in any application, but they may be considered as accelerated tests. In addition, they indicate the particular characteristics of a given type of corrosion or the relative efficiency of different types of terminals, with respect to either materials or construction.

Electrical measurements of resistance have been made using both alternating and direct current, with the currents varying from ten microamperes up to those substantially greater than the normal rating of the wire. Resistance

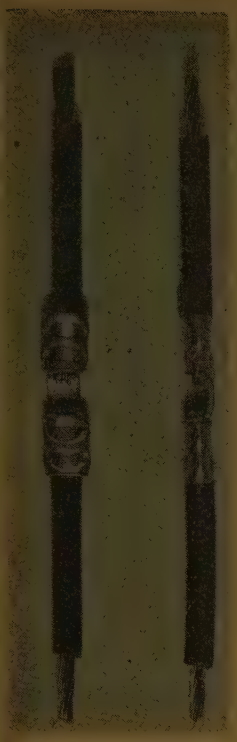


Figure 4. Detail of connector

measurements of this type generally fall in the range from one milliohm to one microhm. The measuring circuits that have been used include the Kelvin–Thomson bridge, potentiometer, and comparison methods. In general, any of these methods may be used with either alternating or direct current. For relatively low resistances with low currents the use of alternating current is to be preferred, in view of the facility with which sensitivity may be increased by amplification. The use of alternating current also eliminates difficulties due to thermal and contact-voltage effects. For the measurement of very low voltages, the use of a tuned circuit such as a wave analyzer is desirable. In general, any of the methods will give a sufficient precision and accuracy with appropriate equipment and calibration.

Test Results

Measurements of the electrical resistance of solderless terminals have been made using different types of corrosion tests with different contact metals over very wide ranges of current, under different temperature conditions for both the measurements and the corrosion tests.

Resistance of Solderless Terminals Over a Wide Range of Current

Measurements of electrical resistance of solderless terminals over a range of current from ten microamperes to ten amperes in decade steps are given in Table I. These measurements were made on an assembly of 500 connectors as shown in Figure 3. The connector is shown in Figure 4; each connector includes two regular crimped solderless terminals with a short strip or link between the individual terminals. Thus, the assembly in Figure 3 consists of a series connection of 1,000 solderless terminals or crimped joints. The connectors are number 16–14 with 19-strand number-14 American Wire Gauge insulated tinned-copper wire. The connectors as shown in Figures 4 and 5 are joined with a short length of insulated wire to give a seven-eighths-inch length of wire between adjacent faces of the connectors. The resistances in ohms in Table I are indicated by R_1 in Figure 5, and include one crimp plus a $^7/_{16}$-inch length of stranded number-14 wire plus one half of the link between connectors. The type of corrosion used in this test consisted of a combination of four per cent salt spray for a period of 24 hours followed by an equal period in a circulating-air oven at 100 degrees centigrade.

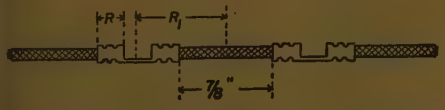


Figure 5. Diagram of measured resistances—connector

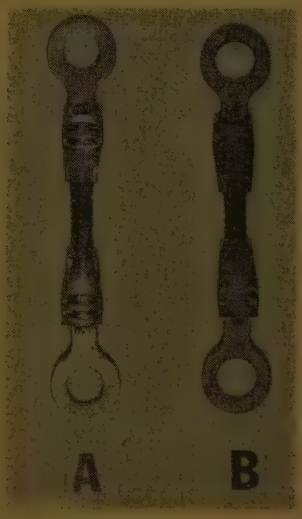


Figure 6. Solderless terminals for resistance measurements during corrosion tests

The results in Table I were measured at room temperature (20 degrees centigrade), and at an elevated temperature approximating 100 degrees centigrade. In the current range from one milliampere to ten amperes the resistance measurements were made using alternating current at a frequency of 60 cycles per second, and below this current range they were at a frequency of 100 cycles per second. The test results show that the resistance of the solderless terminals is independent of the current over the range from ten microamperes to ten amperes. This applies to resistance measurements at both room temperature and at 100 degrees centigrade. In this case the corrosion test was continued for a period of 108 days, which would normally be considered an extreme in terms of standard accelerated corrosion tests.

Comparison of Tinned-Copper Solderless Terminals and Wire With Corrosion-Resistant-Treated Solderless Terminals

The resistance measurements shown in Tables II and III give a comparison of resistance stability during corrosion of a tinned-copper terminal crimped to tinned-copper wire (Table II) with a corrosion-resistant-treated copper terminal and tinned-copper stranded wire (Table III). These resistance measurements were made using two solderless terminals mounted back to back, as shown in Figure 6. The terminals are of a type with an insulation support. The terminals are number 22-16 with seven-strand number-18 insulated wire. The resistances in ohms given in Tables II and III are indicated by R_1 in Figure 7 and include two crimped or solderless terminals plus five-eighths inch of wire plus a portion of each lug of each terminal. The corrosion cycle consisted of passing 15 amperes alternating current,

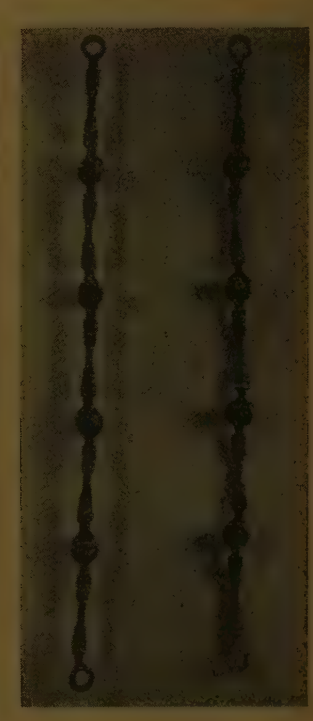


Figure 8 (left). Extent of corrosion—28-day salt-spray test

Figure 9 (right). Series connection of solderless terminals

Table I. Resistance of Solderless Connectors at Currents Indicated

Duration of Corrosion Test—Days	Microamperes		Milliamperes			Amperes	
	10	100	1	10	100	1	10
Resistance Measurements at Room Temperature (20 Degrees Centigrade)—Ohm							
0	0.00019	0.00019	0.00019	0.00019	0.00018	0.00018	0.00018
8	0.00019	0.00019	0.00019	0.00019	0.00019	0.00019	0.00019
18	0.00019	0.00020	0.00020	0.00020	0.00020	0.00020	0.00020
28	0.00019	0.00020	0.00020	0.00020	0.00020	0.00020	0.00021
33	0.00019	0.00020	0.00020	0.00020	0.00020	0.00021	0.00021
42	0.00020	0.00020	0.00020	0.00020	0.00020	0.00020	0.00020
49	0.00020	0.00021	0.00021	0.00021	0.00021	0.00021	0.00021
55	0.00021	0.00021	0.00021	0.00022	0.00021	0.00021	0.00021
62	0.00021	0.00022	0.00022	0.00022	0.00021	0.00021	0.00021
69	0.00021	0.00022	0.00023	0.00023	0.00022	0.00022	0.00021
76	0.00022	0.00024	0.00024	0.00024	0.00022	0.00022	0.00022
83	0.00025	0.00025	0.00025	0.00025	0.00022	0.00022	0.00022
90	0.00025	0.00025	0.00025	0.00025	0.00023	0.00023	0.00023
98	0.00024	0.00024	0.00024	0.00024	0.00023	0.00023	0.00023
108	0.00024	0.00023	0.00023	0.00023	0.00024	0.00024	0.00024
Resistance Measurements at Approximately 100 Degrees Centigrade—Ohm							
49	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028	0.00027
55	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028	0.00027
62	0.00028	0.00028	0.00028	0.00028	0.00028	0.00027	0.00026
69	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028	0.00028
76	0.00028	0.00029	0.00028	0.00028	0.00028	0.00028	0.00028
83	0.00028	0.00028	0.00029	0.00028	0.00028	0.00028	0.00028
90	0.00030	0.00030	0.00030	0.00030	0.00029	0.00029	0.00029
98	0.00030	0.00030	0.00030	0.00030	0.00029	0.00029	0.00029
108	0.00030	0.00030	0.00020	0.00020	0.00032	0.00032	0.00031



Figure 10. Solderless terminals

A—Before corrosion test
B—After 28-day corrosion test

60 cycles per second, through the terminals for 45 minutes, followed by $7\frac{1}{4}$ hours in ten per cent salt spray, after which the salt spray was shut off, although the samples were allowed to remain in the salt-spray chamber for the remainder of the 24 hours. Certain standard specification tests have used a similar corrosion cycle. It has been found for the high-current portion of the corrosion cycle that, if the tongue of the corroded terminals is adequately cleaned for contact purposes, no perceptible heating of the terminals will ensue. However, if contact with corroded terminals is made without cleaning of the contact points where current enters the terminals under test, excessive heating of the terminals will occur. These temperatures may be in the range of 100 to 200 degrees centigrade or higher, depending upon the extent of the corrosion where contact is made. However, such heating is at the contact points where current is brought into the terminal assembly and is not due to the resistance of the solderless terminal itself. For these particular tests groups of pairs of terminals, shown in Figure 6, were connected in series by bolted connections, as shown in Figures 8 and 9. Figures 8 and 9 show the extreme corrosion products formed after a period of 28 days or 28 corrosion cycles. A considerable portion of the corrosion products that are shown results from the plated iron machine screw and nut that were used to bolt the terminals together. The resistance measurements given in Tables II and III were made across each pair of terminals, as shown in Figure 6. The results indicate a practically constant electrical resistance for the corrosion-resistant terminals throughout the corrosion test, as compared to an approximate 75 per cent increase for the regular tinned-copper terminals. If allowance is made in the resistance measurements for the approximate resistance of the connecting wires between the two terminals, and of the resistance for a portion of the tongue of the terminals, the terminal resistance itself, as indicated by R in Figure 7, is of the order of 0.00008 ohm for the tinned-copper terminals, and 0.00005 ohm for the corrosion-resistant-treated terminals. Thus, the corrosion-resistant-treated terminal not only shows greater conductivity stability during the corrosion test, but also gives a decreased initial resistance per crimped or solderless terminal. The corrosion conditions existing in this particular test are undoubtedly very severe, and probably are very much more severe than would be encountered in any practical application.

Conductivity Stability of Relatively Large Solderless Terminals Under Corrosion

The foregoing measurements of the electrical resistance of solderless terminals during corrosion have been made on stranded wire sizes 14 and 18 with corresponding terminals. Tests also have been made on stranded wire sizes 4 and 6 with corresponding terminals. These tests have been made on two terminals mounted back to back as shown in Figure 10. Figure 10, part *A*, shows the terminal before the start of the corrosion test, and Figure 10, part *B*, shows the terminal after 28 days' salt-spray corrosion.

Figure 11 shows the resistance of the terminals and wire as a function of corrosion time or cycles during the test and includes:

Graphs 1 and 3, tinned-copper terminals with tinned-copper wire.

Graphs 2 and 4, corrosion-resistant-treated copper terminals with tinned-copper wire.

Graphs 1 and 2, number 6 terminals with stranded (181 strands) wire.

Graphs 3 and 4, number 4 terminals with stranded (132 strands) wire.

The corrosion cycle consisted of eight hours of four per cent salt spray and 16 hours of circulating-air oven at 100 degrees centigrade. The resistances are measured in a manner corresponding to the diagram shown in Figure 7, and in-

Table II. Resistance of Solderless Terminals—Tinned-Copper Terminal—Tinned-Copper Stranded Wire

Duration of Corrosion Test—Days	Ohm				
	No. 1	No. 2	No. 3	No. 4	Average
0	0.00046	0.00046	0.00052	0.00046	0.00048
2	0.00046	0.00046	0.00052	0.00046	0.00048
3	0.00048	0.00047	0.00054	0.00050	0.00050
8	0.00050	0.00051	0.00054	0.00050	0.00051
12	0.00057	0.00052	0.00054	0.00055	0.00054
14	0.00055	0.00052	0.00054	0.00056	0.00054
16	0.00056	0.00052	0.00056	0.00056	0.00055
23	0.00072	0.00051	0.00054	0.00061	0.00060
28	0.00096	0.00062	0.00096	0.00067	0.00080

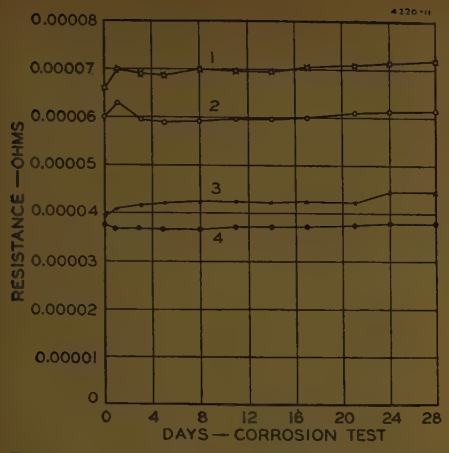


Figure 11. Electrical resistance–corrosion time of corrosion-resistant tinned terminals

Table IV. Resistance of Solderless Terminals—Strand to Terminal

Strand Number	Duration of Corrosion Test—Days						
	0	13	27	34	44	50	57
1	0.0036	0.0037	0.0038	0.0036	0.0039	0.0035	0.0035 ohm
2	0.0040	0.0040	0.0041	0.0045	0.0041	0.0039	0.0038 ohm
3	0.0038	0.0039	0.0041	0.0041	0.0041	0.0039	0.0039 ohm
4	0.0038	0.0038	0.0039	0.0040	0.0040	0.0037	0.0035 ohm
5	0.0038	0.0038	0.0038	0.0037	0.0039	0.0037	0.0036 ohm
6	0.0038	0.0039	0.0038	0.0037	0.0041	0.0039	0.0037 ohm
7	0.0037	0.0038	0.0038	0.0037	0.0039	0.0036	0.0035 ohm
8	0.0041	0.0043	0.0044	0.0044	0.0045	0.0042	0.0041 ohm
9	0.0041	0.0044	0.0045	0.0044	0.0046	0.0044	0.0042 ohm
10	0.0038	0.0041	0.0043	0.0043	0.0042	0.0041	0.0039 ohm
11	0.0038	0.0042	0.0043	0.0045	0.0043	0.0041	0.0039 ohm
12	0.0041	0.0043	0.0044	0.0044	0.0045	0.0043	0.0041 ohm
13	0.0039	0.0041	0.0042	0.0041	0.0043	0.0040	0.0039 ohm
14	0.0039	0.0043	0.0043	0.0042	0.0044	0.0041	0.0039 ohm
15	0.0040	0.0046	0.0045	0.0045	0.0046	0.0044	0.0042 ohm
16	0.0039	0.0043	0.0043	0.0043	0.0044	0.0041	0.0039 ohm
17	0.0039	0.0043	0.0043	0.0044	0.0044	0.0042	0.0040 ohm
18	0.0038	0.0042	0.0043	0.0043	0.0044	0.0041	0.0040 ohm
19	0.0040	0.0043	0.0045	0.0045	0.0045	0.0043	0.0041 ohm

clude two terminals plus one-half-inch to five-eighths-inch wire, plus portions of the tongue of each terminal. The results show relatively good conductivity stability for both sizes of wire and for both types of terminals, the best stability being obtained with the corrosion-resistant-treated terminals. Furthermore, the resistance of the corrosion-resistant terminal is significantly less than for the tinned-copper terminal. Making appropriate allowance for the resistance of the wire and tongue in the measurements given in Figure 11 gives a value for the initial barrel resistance (R in Figure 7) of the number-4 and number-6 tinned-copper terminals of approximately 0.000014 and 0.000009 ohm, respectively, and of the number-4 and number-6 corrosion-resistant terminals 0.000011 and 0.000007 ohm, respectively.

Resistance Measurements on Individual Strands of a Stranded Wire With a Solderless Terminal

Previous resistance tests on solderless terminals have been made from one terminal to another terminal. In this case, however, resistance measurements have been made from each strand of a stranded wire to the solderless terminal crimped to the group of stranded wires. The purpose of these tests is to determine the uniformity of the resistances from the different strands to the terminal. Measurements were made on an assembly, as shown in Figure 12, with the results being given in Table IV. A wire with 19 strands was used in which strands 1–12 inclusive were in the outer layer, 13–18 inclusive were in the intermediate group, and 19 was the central strand. The results in Table IV give the resistance measurements made on each strand at different lengths of time during a 57-day corrosion test. The corrosion test consisted of a cycle of 24 hours in four per cent salt spray followed by 24 hours in a 100 degrees centigrade circulating-air oven. In measurements of this type it is not possible to obtain a high degree of uniformity among the individual measurements shown in Table IV in view of the high resistance of the individual strand and the variable length of the individual strands. However, the results do indicate both a reasonable uniformity of individual-strand resistance measurements and a satisfactory conductivity stability for the individual strands.

Effect of Different Contact Metals With Copper

Previous test results have been obtained using regular tinned-copper terminals crimped to tinned-copper wire. The results in Figure 13 gave a comparison of the electrical resistance as a function of corrosion time, using graph 1, copper terminal with tinned-copper stranded wire; graph 2, tinned-copper terminal and tinned-copper stranded wire; graph 3, corrosion-resistant-treated terminal and tinned-copper wire. These tests were made using number-22-16 terminals with seven-strand number-18 wire. The measurements were made on two terminals mounted back to back, as shown in Figure 6, and include the resistance R_1 as indicated in Figure 7. The corrosion-test cycle consisted of four per cent salt spray for eight hours, followed by 16 hours in a circulating-air oven at 100 degrees centigrade. The test was continued for a period of 28 days. The curves in Figure 13 show that the greatest increase in resistance was obtained with the copper terminal on tinned-copper wire (graph 1). This increase in resistance may be considered to be due to the oxidation of the copper and to the galvanic action resulting from the two different contact metals. The greater conductivity stability of the tinned-copper terminal to the tinned-copper wire (graph 2) as compared to graph 1, copper terminal to tinned-copper wire, is due both to the absence of any electromotive force between the different contact metals and to the greater chemical stability of the tin. Graph 3 shows the improved conductivity stability of the corrosion-resistant-treated copper terminal and tinned-copper wire. This is due to the fact that both corrosion and contact electromotive forces due to different metals have been effectively eliminated in this terminal. It is also of interest in studying Figure 13 to note that the terminals with the greater conductivity stability with corrosion time have the lower initial resistance. Since these differences in the initial resistances are due to differences in the barrel resistances, R of Figure 7, and since the initial resistances include some wire resistance, the differences between the barrel resistances are proportionally much greater than those indicated.

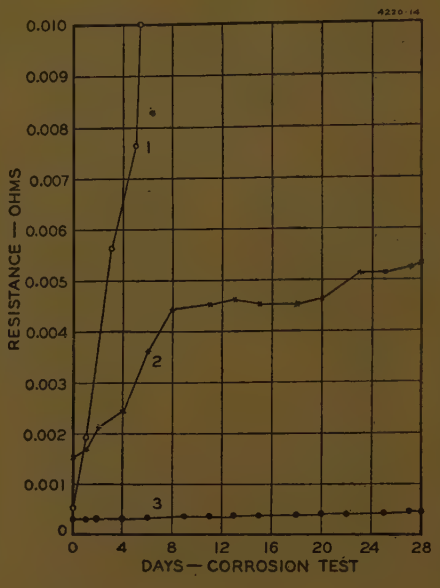
Table III. Resistance of Solderless Terminals—Corrosion-Resistant Treated Copper Terminal—Tinned-Copper Stranded Wire

Duration of Corrosion Test—Days	Ohm						
	No. 1	No. 2	No. 3	No. 4	No. 5	No. 6	Average
0	0.00043	0.00042	0.00043	0.00042	0.00043	0.00043	0.00043
1	0.00043	0.00043	0.00043	0.00042	0.00043	0.00043	0.00043
4	0.00043	0.00043	0.00043	0.00042	0.00042	0.00043	0.00043
6	0.00040	0.00040	0.00040	0.00038	0.00040	0.00040	0.00040
8	0.00041	0.00041	0.00043	0.00038	0.00041	0.00041	0.00041
11	0.00041	0.00041	0.00042	0.00040	0.00040	0.00042	0.00041
13	0.00042	0.00040	0.00042	0.00038	0.00040	0.00041	0.00040
17	0.00041	0.00041	0.00040	0.00039	0.00040	0.00041	0.00040
19	0.00041	0.00040	0.00041	0.00039	0.00041	0.00040	0.00040
21	0.00041	0.00041	0.00042	0.00039	0.00040	0.00042	0.00041
28	0.00041	0.00041	0.00041	0.00036	0.00038	0.00041	0.00040



Figure 12. Assembly — electrical measurements strand to solderless terminal

Figure 14 (below). Electrical resistance—corrosion time of different contact metals—aluminum wire



The Effect of Different Contact Metals With Aluminum

The curves in Figure 14 give the relative conductivity stability of different types of terminal metals with stranded aluminum wire. Graph 1 shows the results with a tinned-copper terminal with aluminum wire; graph 2 shows an aluminum terminal with aluminum wire; graph 3 shows a specially treated terminal and aluminum wire. These tests were made using number-22-16 terminals with seven-strand number-18 wire. The terminals were mounted back to back as shown in Figure 6. The resistance in ohms in Figure 14 is shown by R_1 in Figure 7 and includes portions of each lug plus two crimps, plus five-eighths inch of wire. The corrosion cycle consisted of eight hours of four per cent salt spray and 16 hours of circulating-air oven at 100 degrees centigrade. The greatest increase in resistance is obtained with the tinned-copper terminal to the aluminum wire, since in this case the increase in resistance is due both to galvanic action because of different contact materials and to the oxidation of the aluminum. The aluminum terminal to the aluminum wire shows an initial rapid increase in resistance, because of the rapid formation of relatively high-resistance aluminum oxide. However, the rate of aluminum-oxide formation decreases as a protective coating is formed on the wire, thus giving somewhat greater resistance stability after eight days of the corrosion test. The greatest resistance stability is obtained with the treated terminal and aluminum wire, since in this case galvanic action is eliminated, and the chemical corrosion stability has been increased.

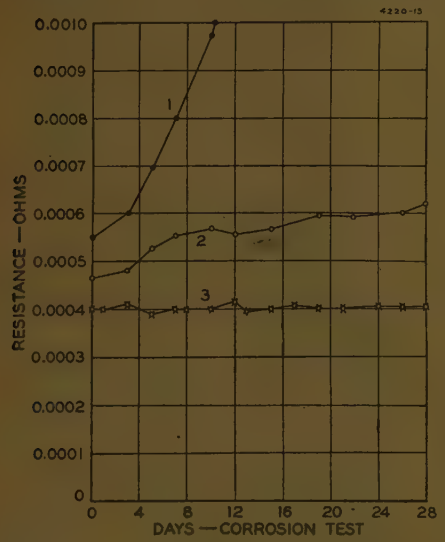


Figure 13. Electrical resistance—corrosion time of different contact metals—tinned copper wire

The Effect of Temperature During the Corrosion Cycle

The curves in Figure 15 give the relative conductivity stability of different types of terminals when subjected to a corrosion cycle consisting of salt spray alternating with circulating-air oven at various temperatures. The graphs marked *A* are the results obtained using tinned-copper terminals with tinned-copper wire; the graphs marked *B* are corrosion-resistant-treated terminals with tinned-copper wire. The temperatures used with the tinned-copper terminals and wire are 25, 66, 100, 133, 166, and 200 degrees centigrade. The temperatures used with the corrosion-resistant-treated terminals were 100, 133, and 166 degrees centigrade. These results were obtained with number-22-16 terminals and seven-strand number-18 wire with the terminals mounted back to back as shown in Figure 6. The resistance in ohms in Figure 15 is indicated by R_1 in Figure 7 and includes portions of each lug plus two crimps plus five-eighths inch of wire. The corrosion cycle consisted of eight hours of four per cent salt spray

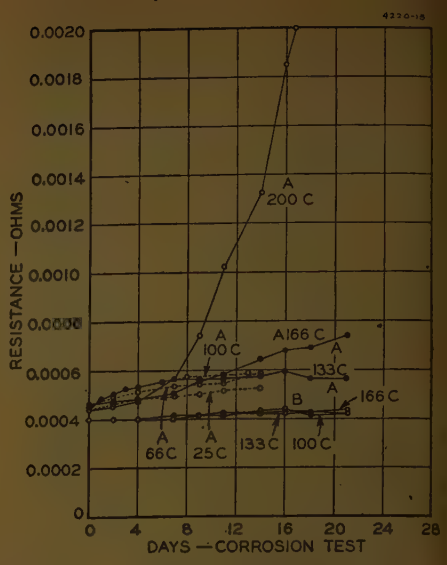


Figure 15. Electrical resistance—corrosion-time effect of different temperatures

followed by 16 hours in a circulating-air oven at the temperatures indicated. The results of electrical-resistance measurements as a function of corrosion time show very good stability at all temperatures for the corrosion-resistant-treated terminals. For the tinned-copper terminals, the conductivity stability is satisfactory for all temperatures, except the highest ones. The effects of the high temperature are both an increase in the corrosion rate, and the formation of oxides having relatively high specific resistance.

Conclusions

1. Solderless terminals may be designed to be both chemically and electrically stable under corrosion conditions of salt spray and heat. It is believed that these corrosion conditions represent reasonably severe operating conditions.

2. The contact metals should have maximum chemical corrosion stability and a minimum electropotential between them for improved performance.

3. The tensile strength of the stranded wire to the crimped joint of the solderless terminal may be made approximately equal to that of the stranded wire when terminated in a solderless terminal.

4. The electrical resistance of the solderless terminal is independent of the magnitude of the current. Tests made on number-18 American Wire Gauge tinned-copper stranded wire with corresponding solderless terminal gave electrical resistance independent of the current over the range from ten microamperes to ten amperes at both room temperature and 100 degrees centigrade.

5. The electrical resistance of the solderless terminal is less than that for an equal length of the corresponding stranded wire. This should insure satisfactory operating performance with respect to temperature rise under load.

Electrical Control in Automatic Pilots

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Synopsis: Engineers generally have been encouraged to attack new seemingly difficult and complicated problems by adapting the general principles utilized in their peacetime work to specific situations arising from the changed methods of modern warfare. This is well exemplified in the solution of aircraft-automatic-control problems.

A summary of automatic-pilot principles and a description of some of the unique and less understood features of a specific modern design are discussed in this paper. It is of especial interest at the present time, because many manual operations of flight and engine control are being studied with a view to making them automatic, and, because as this trend becomes more pronounced, the interconnection of these automatic features will make the problems of each interrelated.

THE automatic pilot is, as its name implies, a mechanism capable of automatically piloting an aircraft and maintaining it on a course, despite disturbances in the surrounding air.[1] Such terms as "relief pilots," "maneuvering pilots," "hard or soft pilots," are used to classify apparatus which accomplishes this result. Relief pilots are intended to relieve the strain on the human pilot mainly during straight level flight and are more simple than the maneuvering type of pilot. Hard pilots are designed to hold a given course or attitude rigidly without consideration for the comfort of the passengers and crew. The soft pilot, on the other hand, holds the general course and attitude but without uncomfortably jarring the personnel. Some designs of automatic pilots incorporate all the features of relief, maneuvering, and hard or soft control.

It is highly desirable to have the automatic pilot under the control of the human pilot at all times, so that it serves as his assistant to relieve him of the routine operations of flying. He is thus able to devote more attention to other problems, such as engine control and aerial navigation. The advantage of relieving the human pilot in this manner has been emphasized in the larger airplanes where increased personnel have been required despite the additional operations already made automatic. As more of the duties are performed by an "automatic crew" under human guidance, the human crew will perform its work more proficiently and with less fatigue.

There are a number of requirements which an automatic pilot must meet in order to be considered satisfactory. Primarily it must hold any set course and attitude, within the limits of the aircraft performance, for periods suitable to the navigational problems involved. In accomplishing this it must be stable. It must neither cause the control surfaces to flutter nor create hunting of the aircraft of a magnitude that would be noticeable to a passenger. Ease of operation, both in turning the pilot "off" and "on" and in changing course or attitude, is an important factor to the human pilot. Ease of overhaul and repair is



Figure 1. A gyroscope tends to maintain a fixed direction in space

equally important to the maintenance crew. As is the case with all equipment for aircraft, light weight and small size are essential, especially in parts mounted on the instrument panel. The automatic pilot should "fail safe," that is, failure of any of its components should not result in violent motion of the airplane. In addition, the automatic pilot must be disconnected easily and overpowered readily.

The fundamental operation of an automatic pilot is relatively simple. Basically its operation is analogous to that of a human pilot. When an error in the course or attitude of the plane is detected, both the human pilot and automatic pilot move the control surfaces in such a way as to reduce this error and, so that the resulting motion of the aircraft is stable; that is, correction of course and attitude errors is accomplished smoothly and without overshooting.

Detection of an error in course or attitude requires a reference. Gyroscopes are used almost universally as references for automatic pilots, since they tend to maintain a fixed direction in space, as shown in Figure 1. They also may be used as references by the human pilot when the ground is not visible. Course and attitude errors may be determined by measuring displacement of the airplane with respect to these gyroscopic references. The measuring devices which are known as error detectors or pickoff units correspond to the senses of a human pilot. The signal from the pickoff unit is modified in the proper manner to achieve stability, amplified, and then transmitted to the control surfaces by means of a servomotor. As shown in the appendix, stable operation of the aircraft requires that the motion of the control surface should be responsive to the velocity, and in most cases the acceleration of the airplane in addition to being responsive to the error signal, that is, anticipation of the airplane's future position, is required if stability is to be achieved.[2,3]

Automatic-Pilot-System Components

In conventional automatic-pilot practice the reference units are usually a directional gyroscope, as shown in Figure 2, with its spin axis horizontal for course reference and a horizon gyroscope, as shown in Figure 3, with its spin axis vertical for the pitch and bank references. The directional gyroscope is subject to random drift due to friction present in the unit as well as drift caused by the rotation of the earth. Consequently it is compared normally at periodic intervals with a magnetic compass and reset if necessary. It is possible, however, to couple the directional gyroscope to a magnetic compass so that it is reset continuously and automatically. A magnetic compass is not normally used for a course reference since it is seriously affected by pitching and rolling of the aircraft. However, if a gyroscope with a vertical spin axis is used to maintain the compass in a horizontal plane, it may be used directly as a course reference.

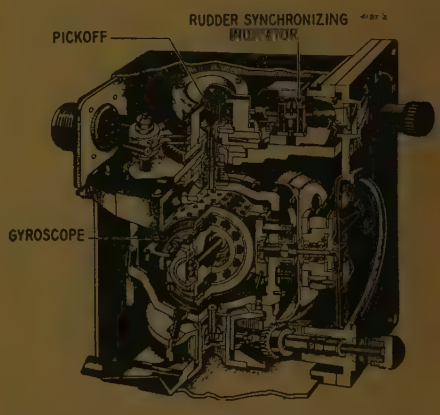


Figure 2. Cutaway view of a directional gyroscopic control instrument

The horizon gyroscope is subject to drift of the same type as that in the directional gyroscope. In this case, however, a pendulous erection system, the average direction of which is the true vertical, is used to correct the instrument.

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The speed of this correction must be sufficiently high to correct for the earth's rotation with a negligible error, but, because of the centrifugal effects on the pendulum during turns, there is an upper limit to the speed at which correction may be applied.

Gyroscopes may be of the air-driven or electrically driven type. The electrically driven units provide better performance at high altitudes, better starting characteristics at low temperatures, and a reduction of the dirt hazard in the instrument. The recent developments of electric gyroscope motors[4] of instrument size has made it practical to mount electrical gyroscopes on the instrument panel, thus utilizing them as flight instruments

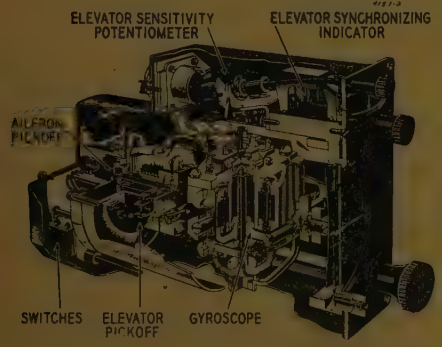


Figure 3. Cutaway view of a bank-and-climb gyroscopic control instrument

as well as the references for the automatic pilot. In these small gyroscopes a-c motors of the squirrel-cage or hysteresis type are preferred to d-c motors because of the elimination of brush-wear and radio-interference problems, as well as the ease of attaining higher speeds. The 400-cycle a-c power system which is rapidly being adopted as standard aircraft power makes it possible to obtain high gyroscopic speeds and a resulting high angular momentum of the gyroscope with conventional four- or two-pole motors of the a-c type.

The motion of the airplane with respect to these references is normally measured with a pickoff unit which provides a control signal proportional to this motion. The older mechanical or air-valve pickoffs in many cases are being replaced by electrical units which may be classified as the direct-contact or potentiometer type, the variable-inductance or E type, and the induction-regulator or Selsyn type. In general, these units are oriented so as to produce an a-c signal, the polarity and magnitude of which correspond to the direction and amount of deviation of the airplane. Typical output voltage characteristics of the induction type are shown in Figure 4.

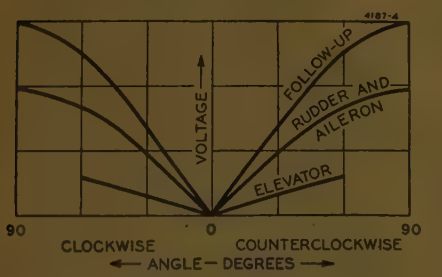


Figure 4. Output voltage characteristic of electrical pickoff and follow-up units of the induction (Selsyn) type

Some of the stabilizing or antihunt signal sources which have been used are as follows:

(*a*). Restrained or rate gyroscopes, if properly oriented, will provide a displacement having a magnitude proportional to the angular velocity of the aircraft about the axis being stabilized.

(*b*). A signal proportional to the position of the control surface or the force on it will contain both acceleration and velocity components of the motion of the aircraft and may be used for stabilization.

(*c*). Accelerometers may be used to obtain acceleration components of the aircraft motion.

(*d*). Various aerodynamic measurements, such as wing-tip velocities, rate of climb, angle of attack, and lift on the wings have been used for stabilizing purposes.

(*e*). The error signal may be operated on directly to obtain derivative components. If this signal is electrical it is possible to perform this operation by means of conventional capacitor–resistance networks.

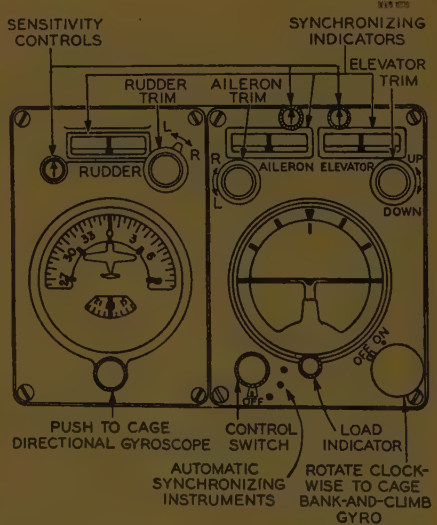


Figure 5. The control knobs combined on the instrument panel of a relief pilot

The problem of automatic stabilization of an aircraft is complicated somewhat by the fact that the system parameters, in particular the inertia, damping constants, and the response of the aircraft to control surface motion vary with loading, altitude, air speed, and so forth. Although it is true that there is considerable interaction between the various axes of an aircraft, particularly the roll and yaw, it has been found that satisfactory relief pilots can be built without taking account of this interaction.

The power units or servomotors for operating the control cables may be classified according to their power source

Past experience with the pneumatic piston type of servomotor has demonstrated that they are operable, but that it is more difficult to stabilize the aircraft because of the compressibility effects in the servomotor.

The hydraulic piston servomotor eliminates these compressibility effects and provides an easily controlled source of power; low-pressure units similar to those shown in Figure 10 are in common use today. These units generally are provided with a by-pass valve for hydraulic disengagement during manual flight. In order to permit the human pilot to take over control in case of an emergency, relief or overpower valves are placed across the piston.

Electric servomotors are usually high speed, of either the a-c or d-c type, driving through a gear reduction to an output pulley. The use of this type of servomotor has been limited somewhat by the relatively high inertia to torque ratio which introduces a time delay, the weight of the control equipment required to obtain variable speed operation, and the complexity of the mechanical disengagement necessary for manual flight. Motors have been designed specially to reduce the inertia to torque ratio; in other instances the motor has been allowed to run continuously, and the load has been controlled by clutching. Saturable reactors also have been used to control the speed of low-power a-c servomotors. Disengagement is usually accomplished by means of solenoid-operated clutches.

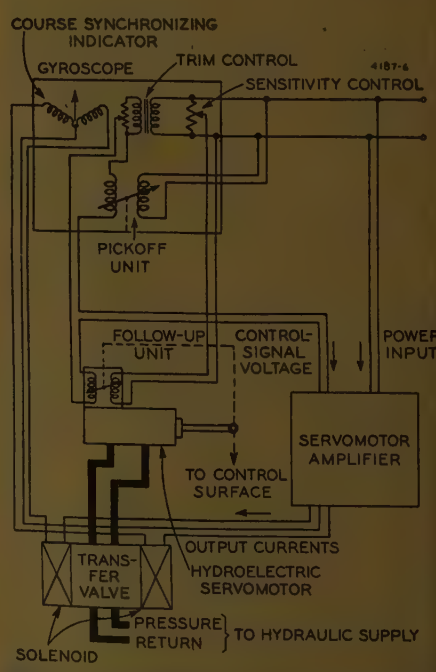


Figure 6. Simplified diagram of one control channel of a relief pilot

The type of amplifier units will depend upon the type of pickoff and servomotor used. For a system using an air-valve pickoff and a pneumatic or hydraulic power servomotor, the fluid flow to the cylinder is controlled by a balanced valve which is actuated by a vacuum-operated diaphragm. The differential vacuum on this diaphragm is controlled by the air-valve pickoffs in the gyroscope. At high altitude, because of the reduction of atmospheric pressure, the force available on the valve is reduced, resulting in reduced velocity of the servomotor.

With an electrical pickoff an electronic amplifier is usually used, first, to discriminate the polarity of the incoming signal, and second, to amplify it to a higher-power level. When this amplifier is used to control an electric servomotor, additional control means, such as saturable reactors or solenoid-operated clutches, are used. Hydraulic servomotor units are controlled conveniently

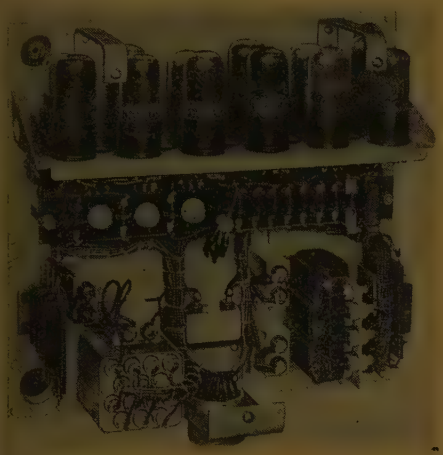


Figure 7. Servomotor amplifier with cover removed

by means of a solenoid-operated balanced oil valve.

Adjustment of the course and attitude of the airplane during automatic-pilot flight, as well as alignment of the automatic pilot with the airplane before engagement, requires controls readily accessible to the pilot. A change in course or attitude of the airplane is usually accomplished by introducing a trim signal into the control circuit, thereby requiring that the airplane change its attitude to restore equilibrium. This trim signal may be introduced mechanically by displacing the rotor of the pickoff with respect to the stator, by electrically displacing the stator signal by means of a selsyn system, or electrically by introducing a bias voltage. It is also desirable to have control of the damping action of the pilot as well as the control switches readily available. In the maneuvering type of pilot the turn and bank controls are combined, so that motion of one control will introduce a rate of turn with the correct amount of bank.

A Typical Autopilot With Electrical Control

The automatic pilot described in detail in this paper exemplifies the basic features of automatic pilots and also illustrates the trend toward electrification of many of its parts. It is a modern design developed specifically as a relief auto-

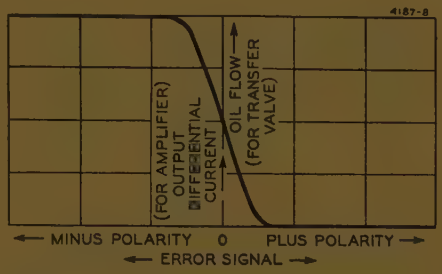


Figure 8. Servomotor amplifier characteristic curves

matic pilot and intended mainly for straight level flight, although capable of performing simple maneuvers. It incorporates recent developments in small electric gyroscopic flight instruments and has fulfilled the need for an automatic pilot having the advantages of electro-hydraulic operation. Its panel mounting dimensions are interchangeable with those of older designs. A simplified diagram of one channel of the system is shown in Figure 6.

The gyroscope references contain small hysteresis-type motors running at a synchronous speed of 12,000 rpm from a 115-volt 400-cycle 3-phase supply. The rotor weight running outside the stator provides exceptionally high gyroscopic stabilizing torques. Figure 2 shows this



Figure 9. A cutaway view of a solenoid-operated transfer valve

motor mounted in gimbals and geared to a vertical dial for use as a directional flight indicator.

The pickoff units are of the induction type having rotors excited directly from a single phase of the 400-cycle supply and stators wound single phase from which the signals are taken. The rotor of the directional pickoff is mounted on the gyroscopic vertical axis, and the stator of this unit is mounted on the frame. Consequently, there is no mechanical friction imposed on the gyroscope by the pickoff unit, since the rotor excitation is already available on the gimbal for motor power. The stator of the pickoff unit is mounted in a ring gear and driven by the knob on the front panel so as to provide a simple means for changing course while in automatic flight.

Figure 3 shows the arrangement of a similar motor used for the bank and climb control. The spin axis is nearly vertical, and a unique electrical erecting device properly damped[5] and actuated by gravity holds it in this position. During turns the centrifugal force acting on the erecting device may cause a slight drift from its correct position. However, a backward tilt of the gyroscopic spin axis has been introduced to compensate correctly for this effect at a rate of turn of approximately 180 degrees per minute. At other rates of turn the compensation is only approximate.

The aileron pickoff unit is mounted



Figure 10. A hydraulic servomotor with an electrical follow-up unit coupled to it

with its primary winding on the main gimbal and its secondary winding on the frame. The elevator pickoff is mounted with its primary winding on the horizon bar linkage, so that it moves with the motor and has its secondary winding on the main gimbal. Typical output voltages from the pickoffs for various angles of deviation are shown in Figure 4.

The secondary windings of the aileron and elevator pickoffs are not rotated easily from outside the gyroscope, so that potentiometer trim controls are added with knobs, as shown in Figure 5, and with connections, as shown in Figure 6.

The compactness and light weight of this pilot result in part from mounting all the controls, with the exception of the "on–off" oil valve, directly on the face of the gyroscope. Separate sensitivity control knobs are provided for rudder-, aileron-, and elevator-damping adjustment. Their electrical connections are shown in Figure 6, and the arrangement of the controls is shown in Figure 5. Small synchronizing instruments are provided in the gyroscopic cases to indicate when the trim knobs are correctly set to

hold the course and attitude that the plane is flying. These instruments read zero when the currents through the transfer-valve solenoids are balanced.

The electric control switch for the automatic pilot is mounted in the lower left-hand corner of the bank and climb gyroscope. It has positions for:

(*a*). The use of the gyroscopes only as flight instruments.

(*b*). The use of the synchronizing indicators for "trimming" the automatic pilot before engagement.

(*c*). The use of all the components in automatic flight.

In addition to these control knobs, the normal caging and dial adjusting knobs of flight indicating instruments are provided.

The control signals for the rudder, aileron, and elevator functions are fed into three separate amplifier channels in the servomotor amplifier shown in Figure 7. The purpose of the amplifier is to con-

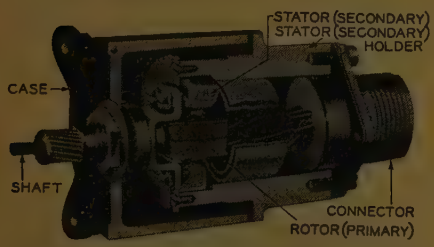


Figure 11. A cutaway view of an electrical follow-up unit

vert the low-level a-c signal input to d-c output of sufficient power to operate the solenoids of the hydraulic transfer valve. Essentially, each channel consists of a balanced two-stage amplifier connected so that the first stage is responsive to the polarity and magnitude of the incoming signal, and the second stage is the power amplifier. With no input signal the output currents of each half of the channel are balanced. With an applied signal the current in one half of the channel will rise, and that in the other half will fall in an amount proportional to the signal voltage; if the signal polarity is reversed the effect on the output current is reversed. The form of the output current versus input signal curve is shown in Figure 8.

The transfer valve shown in Figure 9 is actuated by the output of the servomotor amplifier, and consists of three hydraulically balanced oil valves, each valve being positioned by proportional solenoids located at each end. These solenoids have armatures suspended in radial air gaps without bearings and are spring biased outwardly in the axial direction from their magnetic centers. Differential excitation of the solenoids produces an axial motion of the armatures which operates the valve. These units have been designed so that the rate of oil flow from the valve is approximately proportional to the differential current through the solenoids. This characteristic, together with the linear electronic amplifier characteristic, results in a servomotor velocity approximately proportional to the input signal up to the saturation limit of the amplifier. The arrangement of solenoids at both ends of each valve insures that the valve will fail safe, that is, return to neutral if the electric signal is removed. Conventional hydraulic servomotor units, of a size dictated by the airplane requirements, are used with this equipment.

The stabilizing signal used in this automatic pilot is a voltage proportional to control surface position. It is generated by follow-up units of the induction type similar to the pickoff units. The rotor, which is excited from the same phase of the 400-cycle supply as the corresponding pickoff, is coupled to the servomotor as shown in Figure 10. The signal from the stator of this unit is connected in series with the pickoff signal before it enters the amplifier. The sinusoidal voltage output characteristic of this unit, as shown in Figure 4, is modified by the nonlinear crank-arm coupling, so as to produce an output voltage essentially proportional to the servomotor piston displacement. A cutaway view of this electrical follow-up unit is shown in Figure 11.

Thus the control-signal voltage entering each amplifier channel is the resultant of three components: the trim control voltage for manually setting the attitude of the plane, the pickoff voltage indicating the deviation of the airplane from the set attitude, and follow-up voltage indicating the deflection of the control surface from its neutral position. The resultant input signal determines the amount and direction of unbalance in the output currents and, consequently, the amount and direction of oil flow from the transfer valve.

The typical sequence of operation of the system may be described in the following manner. Assume that the airplane has been flying in a straight course under the control of the automatic pilot so that the pickoff, follow-up, and trim voltages are zero. Under these conditions the amplifier output currents will be balanced, the transfer valve will be in the neutral position, and the servomotor will be stationary. Now if it is suddenly displaced from its course by an outside force, a signal immediately appears on the gyroscope pickoff thereby disturbing the resultant zero-voltage balance of the amplifier input signal. The resulting plate current unbalance in the power tubes of the amplifier causes the proportional oil valve to move from its neutral position thereby permitting oil flow to the servomotor. The servomotor then moves the control surface in the proper direction to return the airplane to its original course. As soon as the control surface moves, a voltage proportional to the amount of this movement is induced in the stator of the follow-up unit. This voltage opposes the voltage induced in the gyroscope pickoff and so reduces the current unbalance from the power amplifier as well as the displacement of the proportional valve. Consequently, motion of the control surfaces continues until these voltages are equal and opposite. At the same time, however, the displacement of the control surface causes the airplane to return gradually to its original course, thereby gradually reducing the pickoff voltage to zero. This reversal of input signal polarity reverses the oil flow to the servomotor, and so the control surface gradually returns to its neutral position. System equilibrium is again established when the control surface is in the neutral position with zero follow-up voltage and when the airplane is set at the correct attitude with zero gyroscope pickoff voltage.

If the electrical trim control in the case of the elevator or aileron control is moved from its neutral position thereby unbalancing the zero signal input voltage, the new equilibrium point will be reached when the airplane attitude has shifted, so that the gyroscope pickoff voltage is equal and opposite to those introduced by the trim control setting and the new follow-up and control surface position required to maintain this attitude.

If the sensitivity control is adjusted so

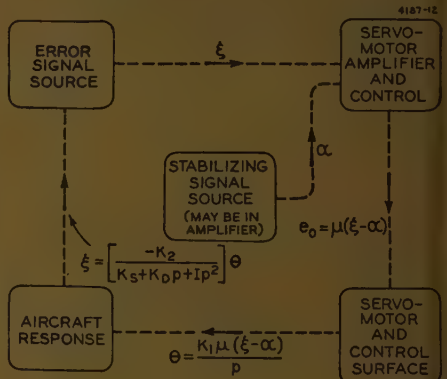


Figure 12. Servomotor system

that the follow-up excitation is decreased, the follow-up signal gradient is reduced, and the control surface must move a greater distance to balance out the pickoff signal. This results in a "stiffer" pilot control.

Samples of this automatic pilot have been used in tests to stabilize airplanes at various altitudes and flying speeds with the indication that the sensitivity control knobs can be set on the ground to a satisfactory position for most normal flying conditions. The stabilization of the airplane as well as the direction held were

found to be well within the desirable limits for this type of automatic pilot.

Conclusions

Automatic pilots are one step toward achieving complete automatic controls for aircraft. This step as a separate problem has been solved satisfactorily in a number of ways that are in general use. One of the major advances in the art has been brought about by the electrification of several components for the automatic pilot, including the gyroscope motors, the error signal pickoff, the follow-up units, and the amplifying units including the transfer valve. These have resulted in better starting characteristics, in more satisfactory operation at high altitude, and in the elimination of mechanical linkages to the instrument panel. The electrical pickoff and follow-up signals have made possible electrical trim controls for adjusting the stability of the aircraft. The use of small potentiometers, together with the recently developed electric gyroscopes of instrument size, allow all of the controls with the exception of the "on–off" oil valve to be mounted on the instrument panel directly in front of the human pilot. The automatic pilot described in detail characterizes all the aforementioned features. Flight-test results have proved the superiority of this design as a relief type of automatic pilot.

Appendix. Theory of Automatic-Pilot Operation

The mathematical study of automatically controlled flight is sufficiently complex to warrant an entire paper. Consequently, only a descriptive analysis will be presented to provide a better understanding of the functions of the various components.

Assume that there is no mutual effect among the three controlled axes, and that the problem of stability is concerned only with small angular deviations. One control axis of the pilot then may be represented schematically by the servomotor system shown in Figure 12.

The component parts may be any of the types mentioned in the paper. When an electrical pickoff is used its time constant is practically zero, and its output can be represented by ξ where ξ is proportional to the error angle.

The stabilizing signal source usually has a small time constant, and its output can be represented by α.

The input to the amplifier is $\xi-\alpha$, and if μ is the amplification the output is $\mu(\xi-\alpha)$. Although the time constants of amplifiers are greater than those of the signal devices, they are negligible compared with those of the airplane in the case of electronic amplifiers followed by solenoid-operated transfer valves.

$$e_0=\mu(\xi-\alpha) \tag{1}$$

This output signal is connected to the servomotor to cause it to move at a rate proportional to the signal. A time constant will appear in the servomotor response, but again this is usually negligible so that if θ is the servomotor and control surface position tending to correct ξ,

$$p\theta=K_1\mu(\xi-\alpha) \tag{2}$$

where K_1 is the servomotor constant.

The displacement of the control surface, θ, from its neutral point produces an approximately proportional torque about the center of gravity of the airplane, so that the airplane accelerates in a direction to decrease ξ. The resulting angular velocity, however, gives rise to damping and static stability torques which limit the maximum velocity. This is the major time constant in the system and consequently cannot be neglected. The response of the airplane to control surface motion then may be expressed as:

$$K_2\theta=-K_s\xi-K_Dp\xi-Ip^2\xi \tag{3}$$

where:

K_s = static stability constant
K_2 = control surface constant
K_D = damping constant
I = angular inertia of plane

If equations 2 and 3 are solved simultaneously, there is obtained:

$$\left[p^3+\frac{K_D}{I}p^2+\frac{K_s}{I}p+\frac{K_1K_2}{I}\mu\right]\xi-\frac{K_1K_2\mu}{I}\alpha=0 \tag{4}$$

With the airplane constants normally encountered in control work and the system gain, $K_1K_2\mu$, required for reasonable accuracy of flight, a stable solution of equation 4 requires that the last term be present in the correct amount. Generally, this term will be of the form:

$$\frac{K_1K_2\mu}{I}\alpha=\frac{K_1K_2\mu}{I}(-Ap-Bp^2)\xi \tag{5}$$

so that equation 4 becomes:

$$\left[p^3+\left(\frac{K_D}{I}+\frac{BK_1K_2\mu}{I}\right)p^2+\left(\frac{K_s}{I}+\frac{AK_1K_2\mu}{I}\right)p+\frac{K_1K_2\mu}{I}\right]\xi=0 \tag{6}$$

By a proper choice of the constants B and A the solution of this expression will be damped properly so as to provide stable operation of the airplane. The constants B and A determine, respectively, the magnitudes of the acceleration and velocity components of airplane motion, $p^2\xi$ and $p\xi$, which are used for stabilization. Various means have been used to obtain these components, but perhaps the most convenient and widely used method is to feed back into the amplifier a signal proportional to the position of the control surface. In this case:

$$\alpha=K_3\theta \tag{7}$$

where K_3 is the feed-back constant.

If equations 7 and 3 are solved simultaneously, there is obtained:

$$\alpha=-\frac{K_3K_s}{K_2}\xi-\frac{K_3K_D}{K_2}p\xi-\frac{K_3I}{K_2}p^2\xi \tag{8}$$

If equation 8 is substituted in equation 4, the equation of motion is:

$$\left[p^3+\left(\frac{K_D}{I}+K_1K_3\mu\right)p^2+\left(\frac{K_s}{I}+\frac{K_1K_3K_D}{I}\right)p+\frac{K_1K_3K_s\mu}{I}+\frac{K_1K_2\mu}{I}\right]\xi=0 \tag{9}$$

Proper stability adjustment is now obtained by adjusting K_3. If the control-surface position is obtained as an electrical signal, K_3 then may be varied easily so as to obtain the desired stiffness of control and flight characteristics for various types of airplanes.

References

1. Works Progress Administration Bibliography of Aeronautics. Supplements to part 21, section 2, on Automatic Pilots.

2. Theory of Servo Mechanisms, H. L. Hazen. *Journal* of the Franklin Institute, September 1934.

3. Technical Notes and Memoranda, National Advisory Committee for Aeronautics.

4. Electrically Driven Gyroscopes for Aircraft, H. M. Witherow, Albert Hansen, Jr. AIEE Transactions, volume 63, 1944, April section, pages 204–08.

5. Damping Cylinders for Aircraft Gyroscopes, F. R. Sias, J. H. Wright. AIEE Transactions, volume 63, 1944.

Peak Voltages With D-C Arc Interruption for Aircraft

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DURING the interruption of direct currents in circuits having inductance, the presence of high-voltage peaks or "inductive kicks" is quite commonly recognized in a qualitative sense. There is however little quantitative data on this subject, particularly at the voltages, loads, and altitudes encountered on modern aircraft.

Data on inductive voltages usually are obtained by taking cathode-ray oscillograms or by observing a cathode-ray oscilloscope. The former method is accurate but relatively slow and tedious; the latter, with the tubes available up to this time, has been somewhat questionable because of the difficulty of clearly determining by eye a single fast transient. In this investigation use has been made of a cathode-ray tube with a retentive screen—so retentive that the trace of a single half-cycle sweep of a 60-cycle wave takes several seconds to disappear. Using this tube it has been possible to take a large amount of data in a short time, so that, instead of relying on a few oscillograms, ten readings could be taken at each point and an average and range established for the voltage disturbances set up.

Scope of This Paper

It is the purpose of this paper to present experimental data on the magnitude and variations of voltage peaks set up by d-c arc interruptions of inductive circuits. These voltages were measured on d-c systems of 30, 60, 125, and 250 volts with currents ranging from 0.1 to 220 amperes and simulated altitudes from sea level to 50,000 feet. The loads used included typical aircraft devices such as motor fields and contactor coils.

The effect of the induced-voltage peaks on insulation puncture or flashover was not investigated. This phase of the subject requires a separate investigation.

Because of limits in time, no attempt is made to explain theoretically the results obtained. Rather, emphasis is on the presentation of data taken to date. An investigation was made, at the same time, of the interrupting ability and the arc time of some of the devices used in these tests, and the results are presented in an accompanying paper by Quill and Rader.[1]

Paper 44-200, recommended by the AIEE committee on air transportation for presentation at the AIEE Los Angeles technical meeting, Los Angeles, Calif., August 29–September 1, 1944. Manuscript submitted June 14, 1944; made available for printing July 12, 1944.

VIRGEL E. PHILLIPS and WALTER P. MITCHEL, both now in the Armed Forces, were employed in the industrial-control engineering division of General Electric Company, Schenectady, N. Y., at the time this paper was written.

Summary

The data presented may be summarized as follows:

1. When a Switchette (the trade name for a small double-break switch with 0.020-inch gaps and opening time of one to two milliseconds) was used on 250 volts and 0.4 ampere at sea level, voltage peaks as high as 28 times line voltage or 7,000 volts were observed.

2. A maximum peak of 130 times line voltage or 3,900 volts was observed while operating the Switchette double-break on 30 volts and 0.4 ampere at sea level.

3. When the Switchette was used the voltage peaks were found to decrease with altitude for all voltages tested. A maximum of seven to one in ratio of voltage at sea level to voltage at 50,000 feet was observed.

4. Using a double-break contactor which has a $^5/_{16}$-inch gap per break and a gap-travel time of 12 milliseconds, the induced voltage peaks rise with altitude in every case. A maximum of two to one in the ratio of voltage at 50,000 feet to voltage at sea level was observed. The loads on which these tests were made, as the text of the paper will show, were very much less inductive than those used for the Switchette.

5. Variations of humidity over a wide range has little effect on the voltage peaks at any altitude.

6. There are no voltage peaks above normal line voltage in a purely resistive circuit. For interruptions which take place in circuits having the same system voltage and the same initial current, the more highly inductive circuits caused the higher-voltage peaks.

7. For any particular voltage lower currents in general give higher peaks. An exception was noted with the Switchette where peak voltage increased as current was reduced down to about 0.4 ampere. A further decrease in current gave a reduction in voltage.

8. For the same voltage and current, peak voltages occurring for double break on the Switchette were as much as 190 per cent of the single-break peak.

9. The breakdown potential of one-fourth-inch-diameter brass electrodes with altitude is shown in Figure 3 of the paper, "Potential Breakdown of Small Gaps Under Simulated High-Altitude Conditions."[2] For an air gap of $^1/_{32}$ inch the breakdown voltage was 4,000 volts at sea level and 1,000 volts at 50,000 feet altitude. Peak voltages higher than those were found in this investigation, indicating the possibility of damage to insulation due to surface flashover such as might occur in an *AN* connector.

10. Frequencies in the order of 50,000 cycles per second were encountered, usually occurring at low currents and at high altitudes. This is a possible source of interference for radio equipment in the immediate vicinity.

Fundamentals of D-C Interruption

The important factors contributing to the current-interrupting ability of a switch in air are the material of the contacts, the contact configuration, the speed with which the contacts open, the final gap length, the condition of the air, and the constants of the electric circuit itself.

If the circuit contains inductance as well as resistance, as it usually does, interruption will result in high voltages

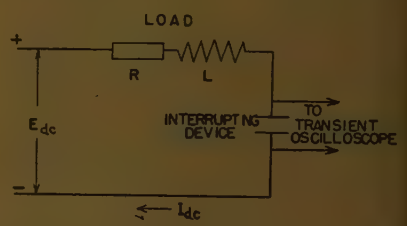


Figure 1. Elementary diagram of test circuit

across the contact tips. Consideration of the interruption on the basis of energy reveals two sources of energy, that stored in the inductance and that supplied by the source of power during the interrupting period. The total energy must be dissipated in the resistance of the circuit and in the arc itself. When the tips first part, an arc will be struck, and hence a low arc voltage will appear. Current will decrease as the gap between tips increases until at some low value it suddenly will go to zero or very nearly so. This last sudden change of current causes a high voltage to be induced across the inductance due to Ldi/dt, and this also must appear across the contacts. This voltage may be enough to cause the gap to break down again with a corresponding decrease in voltage to the arc voltage for that particular gap opening. There may be several of these breakdowns, or, on the other hand, the rate of recovery of the dielectric strength of the air may be faster than the first rise in voltage, and hence an interruption will occur on the first attempt. If the current goes to zero the voltage across the contacts must fall from the peak value to line voltage immediately.

However, indications are that in an appreciable number of cases the current

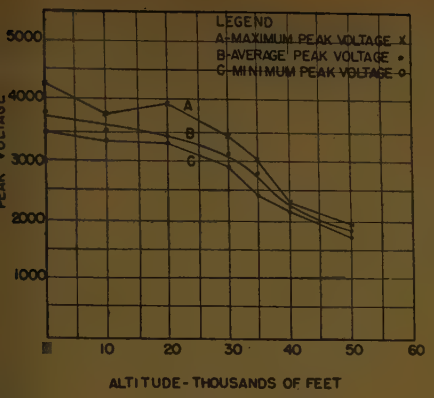


Figure 2. Peak-voltage distribution versus altitude resulting from interruption of an inductive load of 0.4 ampere at 30 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. Ten consecutive readings were taken, and the average, maximum, and minimum readings are shown. Load: inductive *A*, Table I, 0.4 ampere—four coils in parallel

drops suddenly from normal arc current to a very low current which may continue for some time. Thus voltage across the contacts in this case would not fall rapidly from the peak value down to line voltage, but rather only to some intermediate value, and then slowly approach line voltage. This phenomenon is similar to that of the normal and abnormal glow periods as described by Slepian.[3] Also, the glow period following the peak has been noted by the authors in the experimental tests. One common form was for the voltage to drop from the peak down to 300–500 volts and remain for periods of the order of one millisecond, and then taper off to line voltage.

Although the initial conditions of voltage and current are the same before each opening, arc characteristics and peak voltage may differ widely on successive interruptions. Some causes for these variations may be: variations in opening speed, variations in air conditions and the amount of convection action on the arc, and irregularities in the contact surface causing the arc to strike at different points on the contact.

Gap opening except in a limited way and opening speed are factors in the peak voltage reached, but this investigation did not cover these variables. In general, to prevent excessively high peak voltages, a d-c contactor that opens rapidly should not open too wide.[4]

Test Equipment and Procedure

In the measurement of the peak voltages, use was made of a new transient oscilloscope and associated circuit developed in the industrial-control engineering division of the General Electric Company. Its main feature is a high-voltage electronic tube, similar in shape and size to that used in the cathode-ray oscillograph, but having a highly persistent fluorescent screen which allows it to retain the trace of a single transient long enough to be observed by the eye. Thus peaks are read quite easily and accurately, and the wave shape on each interruption can be observed. A resistance–capacitance voltage divider designed to operate independently of the frequency over the range of frequencies involved was used. Every effort was made to keep the leads short and the relative location of the equipment such as to minimize any pickup caused by the high-frequency oscillations at interruption.

The general form of the test circuit is shown in Figure 1. The circuit-interrupting device was placed in a sealed bell jar which could be evacuated. Leads were brought out through sealed terminals at the bottom. The effect of altitude was simulated by reducing the pressure within the bell jar, no attempt being made to control either humidity or temperature. The conversion of pressure to elevation was based on data from National Advisory Committee of Aeronautics report 218.

For the high-humidity test a container of water with an electric-heater attachment was placed within the bell jar and left until water was condensing out around the bottom and on the jar itself. Thus a humidity approaching 100 per cent was attained and held for the duration of this run.

All the data presented in this paper were taken on the following two devices:

1. An aircraft switch known as the Switchette. This is a small double-break switch rated at ten amperes, single circuit, 30 volts, inductive load at all altitudes. It has silver tips of one-eighth-inch diameter and a gap of 0.020 inch. Single-break data were taken using this same Switchette with one contact blocked closed. Hereafter this device will be designated as Switchette single-break or Switchette double-break.

2. A double-break switch with a bridge-type movable contact arm carrying silver tips about one-half-inch diameter on seven-eighths-inch centers. The tip gap was set at 0.312 inch. This unit was a part of a commercial size-1 contactor (*CR*2810–1811) having a tip rating on alternating current of 25 amperes and a motor rating of 7½ horsepower at 550 volts. Hereafter this device will be designated as the double-break contactor.

The loads used are listed in Table I. The inductances given are differential inductances[5] calculated from magnetic oscillograms of current build-up. They serve the purpose of providing a basis of comparison of inductances and hence some indication of the severity of the peak voltages. However, these values should not be accepted as those to use in calculations, as inductance is not a constant but rather a function of the current and rate of change of current for iron-core coils. For example, inductive *A* shows 55 henrys at 50 per cent current and 8 henrys at 100 per cent current. Also

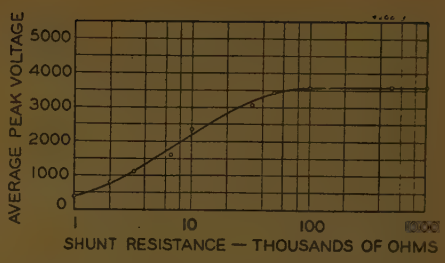


Figure 3. Average peak voltage versus resistance shunted across contacts resulting from the interruption of an inductive load of 0.4 ampere at 30 volts direct current with a double-break Switchette at sea level

Gap: 0.020 inch per break. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.4 ampere—four coils in parallel

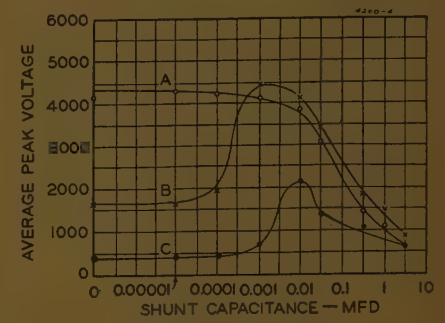


Figure 4. Average peak voltage versus capacitance shunted across contacts resulting from the interruption of an inductive load of 0.2 and 0.4 ampere at 125 volts with a single-break Switchette

Gap: 0.020 inch. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.2 ampere—two coils in series, 0.4 ampere—one coil

A—0.2 ampere—0 feet •

B—0.4 ampere—0 feet ×

C—0.2 ampere—50,000 feet •

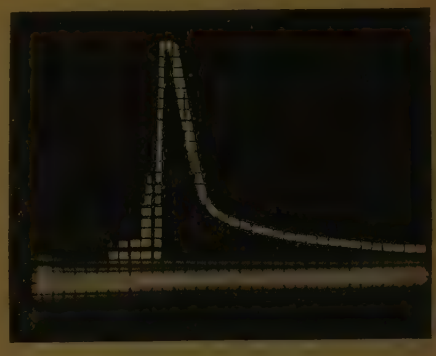


Figure 5. Transient oscilloscope's screen during interruption of an inductive load of 0.2 ampere at 30 volts direct current with the double-break Switchette at sea level

Gap: 0.020 inch per break. Calibration—123 volts per division vertical—one-fourth millisecond per division horizontal. Load: inductive *A*, Table I, 0.2 ampere—two coils in parallel

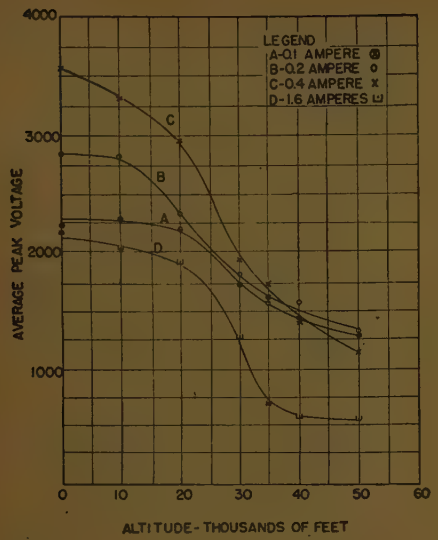


Figure 6. Average peak voltage versus altitude resulting from interruption of an inductive load at 30 volts direct current with the single-break Switchette

Gap: 0.020 inch. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.1 ampere—one coil, 0.2 ampere—two coils in parallel, 0.4 ampere—four coils in parallel, 1.6 ampere—16 coils in parallel

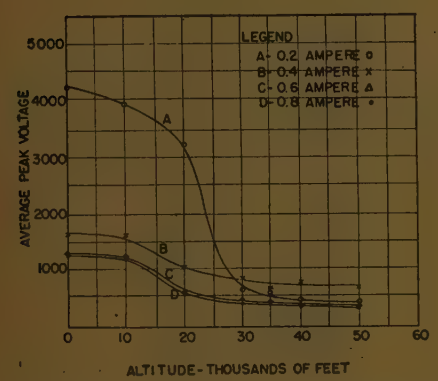


Figure 7. Average peak voltage versus altitude resulting from interruption of an inductive load at 125 volts direct current with the single-break Switchette

Gap: 0.020 inch. The average is taken from ten consecutive readings. Load: inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of above were used for 0.4, 0.6, and 0.8 ampere

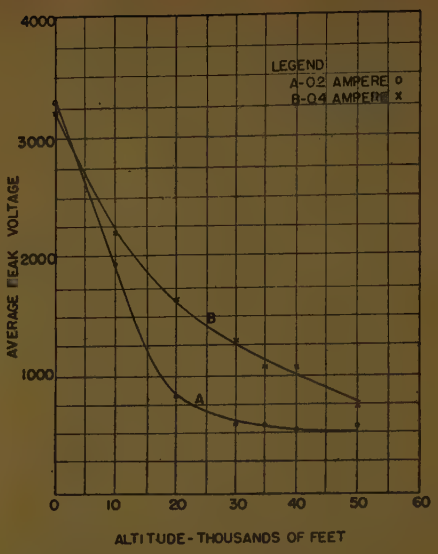


Figure 8. Average peak voltage versus altitude resulting from interruption of an inductive load at 125 volts direct current with the single-break Switchette

Gap: 0.020 inch. The average is taken for ten consecutive readings. Load: inductive *A*, Table I, 0.4 ampere—two coils in series, 0.2 ampere—two coils in series plus external resistance

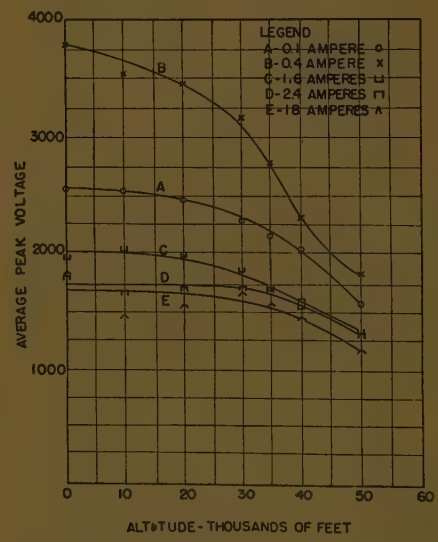


Figure 9. Average peak voltage versus altitude resulting from interruption of an inductive load at 30 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. The average was taken for ten consecutive readings. Load: inductive *A*, Table I, 0.1 ampere—one coil. Parallel combinations of above were used for 0.4, 1.6, and 2.4 amperes. A coil with one-ohm resistance, 360 turns, and 8.5 square inches mean cross-sectional area was used for 18 amperes

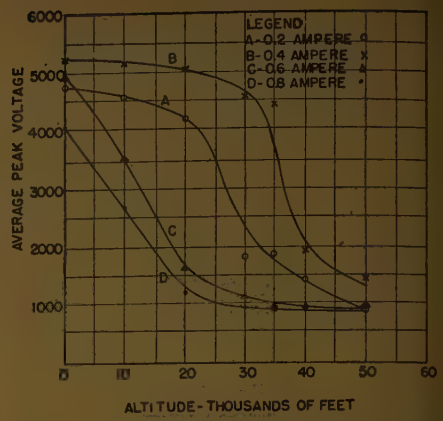


Figure 10. Average peak voltage versus altitude resulting from interruption of an inductive load at 125 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. The average was taken for ten consecutive readings. Load: inductive *A*, Table I, 0.2 ampere—two coils in series. Parallel combinations of above were used for 0.4, 0.6, and 0.8 ampere

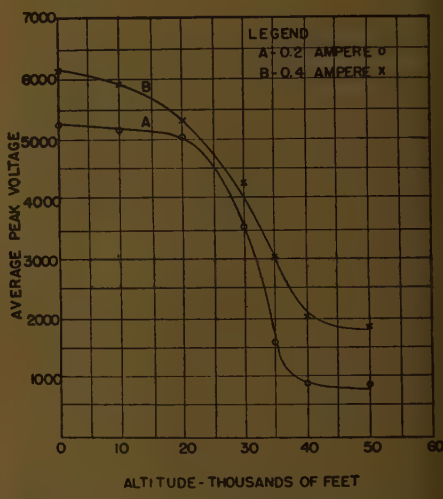


Figure 11. Average peak voltage versus altitude resulting from interruption of an inductive load at 250 volts direct current with the double-break Switchette

Gap: 0.020 inch per break. The average was taken for ten consecutive readings. Load: inductive *A*, Table I, 0.4 ampere—two coils in series, 0.2 ampere—two coils in series plus external resistance

it must be recognized that the inductances found by this method are not the exact values governing current interruption, as the more nearly correct figures would be taken on current decay.

Inductive-*C* load was used for all tests made on the double-break contactor. Current was varied over the range desired by changing a series resistance. Inductive *A* was used for most of the Switchette data. The values shown are for one coil, the desired current at any voltage being obtained by changing the series-parallel combination of these single coils.

As typical aircraft applications inductive-*H*, *J*, and *K* loads were tested. Also used was the shunt field of a one-ninth horsepower aircraft motor, but no inductance figures are given for it.

Data taken previously indicated that peak voltages were higher for a load of a coil with an iron core than for the same coil with an air core. However, peak voltages were much more erratic with the air-core coil, varying as much as two to one on consecutive openings.

Method of Test

The trace on the screen of the transient oscilloscope at each opening of the interrupting device represents the varia-

Table I. Load Inductance

Load Designation	Load	Load Data	Load Rating 100 Per Cent V	Load Rating 100 Per Cent I	Inductance—Henrys at 50 Per Cent I	Inductance—Henrys at 100 Per Cent I
Inductive *A*	Series and series-parallel combinations of d-c industrial relays— data given from each relay	9,600 turns, 1.5-square-inch iron area, Armature sealed	125	0.42	55	8
Inductive *C*	D-c series motor field *MDO*-109 150 horsepower	Series field and commutating field	220	480	0.002	0.0044
Inductive *H*	100-ampere dynamotor contactor coil	2,300 turns, 0.5-square-inch iron area, Armature sealed	28	0.54	2.4	1.5
Inductive *J*	Solenoid-type 200-ampere contactor coil		24	0.42	4.2	1.0
Inductive *K*	Balanced armature-type 100-ampere contactor coil	3,100 turns, 0.09-square-inch iron area, Armature sealed	24	0.19		

on of voltage across the contacts with me. In taking readings, the contacts ere closed long enough to allow the current to reach its steady-state value, and nen the maximum voltage across the ontacts as they opened was recorded. n all the data presented the peak voltages re the average of ten values taken on en consecutive interruptions. A typical istribution of these ten readings is shown n Figure 2. The average values have the dvantage of giving a more uniform curve. lso, any insulation breakdown due to eak voltages which causes surface flashver and tracking probably will be a gradal result of a large number of high voltges which can be represented best by the verage value. Repeat tests taken at a ter date indicate that test points can be eproduced within ten per cent.

ffect of Parallel Resistance or Capacitance

An investigation was made of the effect on voltage peaks of resistance or apacitance shunted across the contacts. n Figure 3 is plotted the variation of eak voltage with shunt resistance over range from 1,000 ohms to one megohm. bove 100,000 ohms, the peaks remained nchanged by an increase in resistance. s the input resistance to the transient scilloscope is two megohms, it is well bove the range at which appreciable ppressing effect is present. This fact is gnificant when it is recalled that, if a agnetic oscillograph is used to measure c voltages, resistance must be placed parallel with the tips of the interrupting evice. The magnitude of this shunt restance is proportional to the system oltage and exerts an appreciable supressing effect on the peak voltage. hus a magnetic oscillograph is usually nsatisfactory for measuring peak voltges, even if the frequency response ere otherwise satisfactory.

In Figure 4, average peak voltage is lotted against capacitance shunted

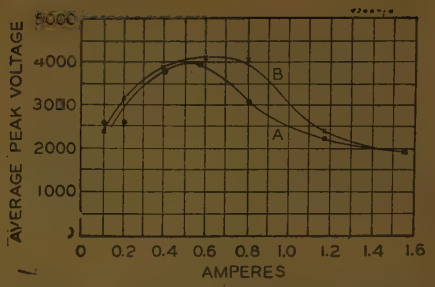


Figure 14. Effect of humidity on peak voltage resulting from interruption of an inductive load at 30 volts direct current with the double-break Switchette at sea level

Load: inductive *A*, Table I
A—Room humidity •
B—High humidity ×

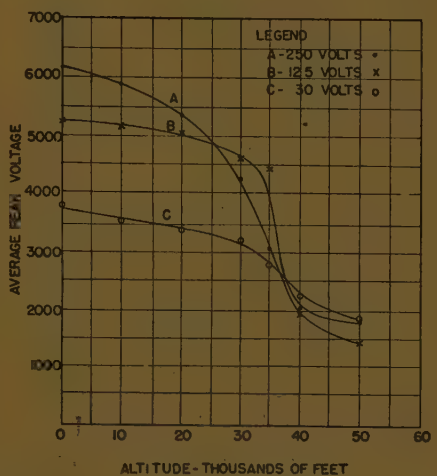


Figure 12. Average peak voltage (ten readings) versus altitude resulting from interruption of an inductive load of 0.4 ampere at 250, 125, and 30 volts direct current with the double-break Switchette

Load: inductive *A*, Table I; gap: 0.020 inch per break

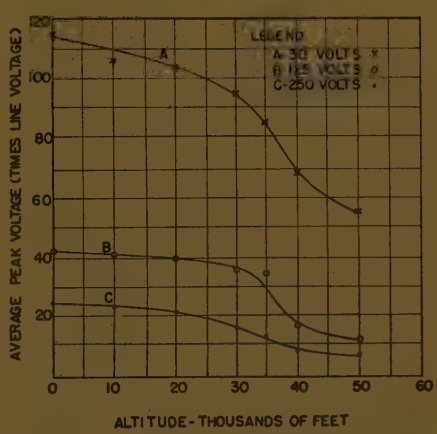


Figure 13. Average peak voltage (in number of times normal line voltage) versus altitude resulting from interruption of an inductive load of 0.4 ampere at 250, 125, and 30 volts direct current with the double-break Switchette

Load: inductive *A*, Table I; gap: 0.020 inch per break

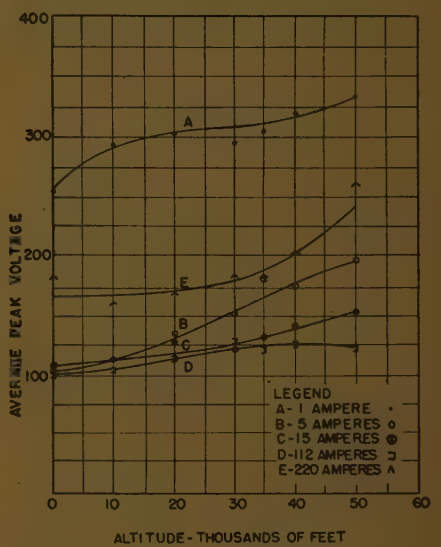


Figure 15. Average peak voltage versus altitude resulting from the interruption of an inductive load at 30 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

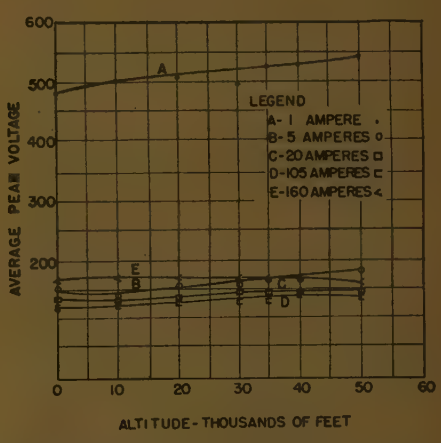


Figure 16. Average peak voltage versus altitude resulting from the interruption of an inductive load at 60 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

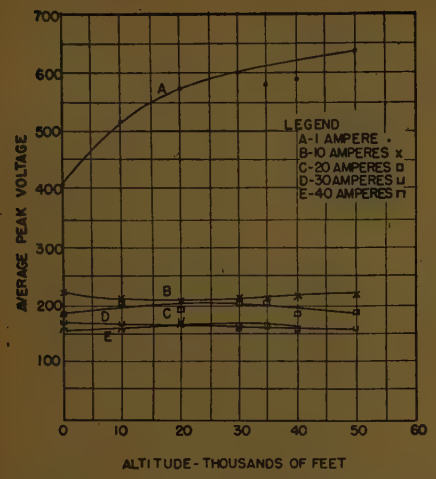


Figure 17. Average peak voltage versus altitude resulting from the interruption of an inductive load at 125 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

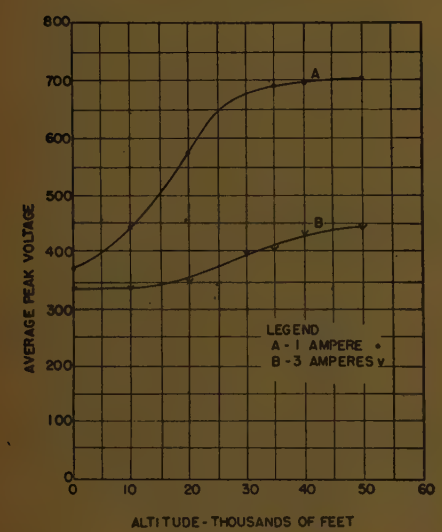


Figure 18. Average peak voltage versus altitude resulting from the interruption of an inductive load at 250 volts direct current with the double-break contactor

Load: inductive C, Table I; gap 0.312 inch per break

across the contacts on an improvised semilog scale. The voltages remain uniform with increase in capacitance until about 0.0001 microfarad. The input capacitance of the transient oscilloscope is approximately 0.00003 microfarad, and hence is well below the critical values. It is interesting to note that two of the curves show a definite rise in voltage, indicating that, while large values of capacitance are effective in suppressing peak voltages, certain critical values may give rise to abnormally high peaks.

Test Results

Wave Shape of Typical Interruptions. In Figure 5 is shown a typical recovery

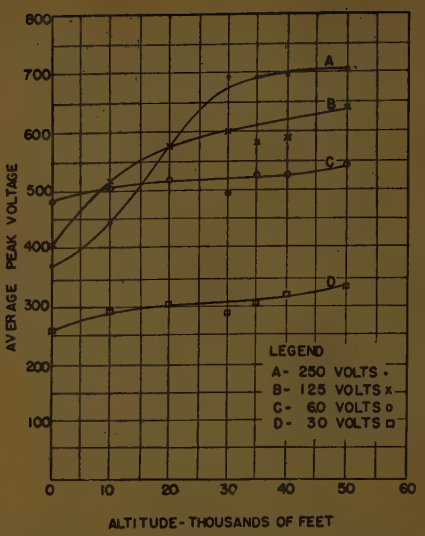


Figure 19. Average peak voltage versus altitude resulting from interruption of an inductive load of one ampere at 250, 125, 60, and 30 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

trace for the Switchette as viewed on the oscilloscope. Although the sharpness of the peak, the number and shape of the preceding oscillations, and the time for decay to circuit voltage varied considerably as circuit constants were varied, this same general shape of the voltage wave during recovery was present in most cases. In a few traces, particularly at 30 volts, a horizontal portion (characteristic of a glow discharge) at a voltage several times line voltage appeared sometimes before and sometimes after the peak. In general the peaks occurring with the double-break contactor appeared much sharper than those with the Switchette, and build-up and decay periods were longer and had less oscillatory disturbance.

Effect of Altitude on Switchette. Figures 6 to 11 show the variation of average peak voltage versus altitude for the Switchette with a 20-mil gap per break for both single-break and double-break operation at 30, 125, and 250 volts direct current on load inductive *A*, Table I. In every case, the peaks are lowered by an increase in altitude, the characteristic variation being that of an S-shaped curve with usually very little change in magnitude of the peaks taking place near sea level or near 50,000 feet. The decrease in magnitude of the peaks at the higher line voltages represents a change of more than five to one for some currents, as altitude is increased from 0 to 50,000 feet.

Effect of Current on Switchette. It should be noted that in most cases as current is increased from a very low value up to 0.4 or 0.6 ampere, the magnitude of the peak increases, after which an increase in current lowers the peaks.

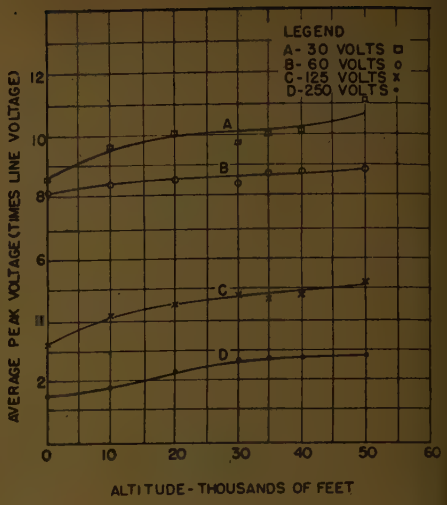


Figure 20. Average peak voltage (in number of times normal line voltage) versus altitude resulting from interruption of an inductive load of one ampere at 250, 125, 60, and 30 volts direct current with the double-break contactor

Load: inductive C, Table I; gap: 0.312 inch per break

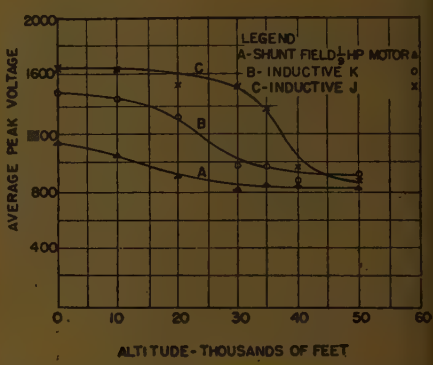


Figure 21. Average peak voltage versus altitude resulting from interruption of inductive loads at 30 volts direct current with the double-break Switchette

Load: aircraft devices now found on 30-volt systems. See Table I

There is some reason, based on tests not reported here, to believe that the magnitude of the peaks again increases as the current approaches some considerably higher value, providing of course that the interrupting limit is not exceeded. This variation of the peaks with current is much more noticeable at sea level than at high altitude.

Effect of Line Voltage on Switchette. In order to show the comparative peak voltages attained with the three different applied voltages at the same current, Figure 12 is plotted. This indicates the comparative severity of the peaks under one of the worst conditions—that is, 0.4 ampere, double-break. The same effect is shown perhaps more spectacularly by Figure 13 in which the ordinates have been changed from absolute voltage to number of times line voltage. In mak-

ng this comparison at different line voltges, it should be noticed that the inuctance values are different. The inuctances at 30 and 125 volts are about he same, whereas that at 250 volts is onsiderably lower.

Effect of Humidity. In order to show he effect of humidity on peak voltages, run was taken at 30 volts, zero altitude nd the results plotted in Figure 14, howing peak voltages as a function of urrent, both at room humidity and a ery high relative humidity (near 100 er cent). According to these results, wide variation in humidity does not ffect materially the magnitude of the eaks.

Effect of Altitude on Double-Break Contactor. To cover higher-current ranges, igures 15 to 18 are presented for the louble-break contactor. It will be seen hat the general tendency is for peak oltages to stay fairly constant or to inrease somewhat with altitude, depending upon the current interrupted. Noteworthy here are the curves for the 60-volt nd 125-volt supplies where the peaks t one ampere jump to two or three times hose at the higher currents. In general, n the current range used here the peaks lecrease with increasing current, though his decrease is small at the higher curents. There is, however, some indication gain of an increase in the peaks at some airly high current (Figures 15 and 16).

The peak voltages versus altitude reulting from the interruption of one amere at the four voltages are shown on igures 19 and 20.

Typical Aircraft Loads. Figure 21 hows the peaks resulting from interrupion of a 30-volt circuit by the Switchette double-break) using inductive loads *J* nd *K* and the shunt field of a one-ninthorsepower motor, all of which are typial aircraft devices. In the first two ases, the current was something above ne-half ampere; and in the latter case, bout one-quarter ampere. It must be ealized, however, that the inductances ealt with here were much smaller, than hose of load inductive *A*. Inductive oad *H* was also tested and gave very early the same peaks as *K*. These eaks must not be construed to be the naximum occurring with any aircraft evice, but were taken as being for typial applications. Other devices, possibly ith critical values of lead capacitance, ight give rise to higher values than for he cases shown.

Conclusions

Experimental data presented in this aper show that voltages of 7,000 volts re possible across the contacts of a witch-interrupting direct current. While he breakdown effect of these peak voltges has not been studied, it is quite posible that they are sufficiently high to ause insulation breakdown or tracking. It was also shown that these peaks are not always worse at altitude.

The two devices investigated gave considerably different results, but this is not surprising, for the devices were not similar. Furthermore, the load ranges and values of inductance differed widely. The subject requires a considerable amount of further investigation.

References

1. D-C Arc Interruption for Aircraft, J. S. Quill, L. T. Rader. AIEE Transactions, volume 63, 1944, December section, pages 883–8.

2. Potential Breakdown of Small Gaps Under Simulated High-Altitude Conditions, M. J. DeLerno. AIEE Transactions, volume 63, 1944, March section, pages 109–12.

3. The Conduction of Electricity in Gases (book), J. Slepian. Westinghouse Technical Night School Press, 1933. Page 151.

4. Gaseous Conductors (book), J. D. Cobine. McGraw-Hill Book Company, Inc., New York, N. Y., 1941. Page 375.

5. Some Aspects of Inductance When Iron Is Present, L. T. Rader, E. C. Litscher. AIEE Transactions, volume 63, 1944, March section, pages 133–9.

Very High-Frequency Radio-Noise Elimination

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THE subject of radio-noise elimination on aircraft has always been a controversial one, even when the frequencies involved were relatively low. With the introduction of very high-frequency equipment the controversy has started afresh. The presence of radio noise in very high-frequency installations has caused observers to forget the principles learned in the work at medium frequency and high frequency, simply because the very high-frequency installation apparently did not follow the same rules that applied to the lower-frequency equipment. It is the purpose of this paper to outline generally the principles of radio-noise elimination at all frequencies, and show how these same principles apply specifically at very high frequency.

Because of the variety of conditions which may be encountered in aircraft radio-noise tests, it will not be possible to detail the test procedures and corrective measures necessary for very high-frequency radio-noise elimination. Rather, it is desired to discuss basic concepts in such a manner that radio-noise test procedure can be made a matter of routine. An outline of test procedure insofar as the engine ignition system is concerned is given, and the remedial measures necessary are generally indicated. The whole procedure is based upon the concept of determining the coupling path into the receiver, with the idea in mind that once this is known the method of eliminating the coupling path is best reached by a consideration of the construction of the airplane under test.

It is unfortunate that many of the terms in use in radio-noise-elimination work are so broad and ill-defined that they cannot be used without careful definition. Since the loose use of these terms has led to some confusion, immediately following a list of definitions is given.

Definitions

A *radio-influence source* is defined as a generator of radio-influence voltages, currents, or fields, and which is capable of exciting a radio receiver in such a way as to produce acoustic output, providing the necessary coupling exists between the radio-influence source and the radio receiver. This implies that the radio-influence source is only influential in providing such an output, and that the measurement of the source is not a measure of the final result.

Radio noise refers specifically to that portion of the acoustic output of a radio receiver produced by coupling a radio-influence source in any manner whatsoever to the radio receiver.

A *coupling source* or *path* is defined as the impedance or circuit element which is excited by the radio-influence source so as to produce acoustic noise in the output of the radio receiver. The coupling source or path may be the radio-influence source in some cases, but may be any part of a circuit, such as a wire, in which radio-influence currents are flowing.

Ground plane—structure ground. Although separated from earth, the metal body of the aircraft acts as a reference plane for the purpose of this paper, and all potentials are in reference to this point. The plane of ground potential is not an obscure hypothetical plane diffi-

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cult to locate as in many earth transmission problems, but is a sharply defined plane of nearly uniform potential existing directly at any metal portion of the structure.

Antenna refers only to the portion of the antenna circuit outside the fuselage, and specifically excepts any portion which is inside the fuselage.

Leadin refers only to the portion of the antenna circuit which is either inside the fuselage or is shielded in any way to prevent undesired coupling of any type.

The *antenna circuit* of a radio receiver is defined as that circuit through which the current flows as a result of signal voltages generated in the antenna due to its presence in a radiation field from a transmitter antenna. As such, it includes the antenna, the leadin (either shielded or unshielded), the receiver input circuit, and the ground-return circuit.

Methods of Radio-Noise Generation

The radio-influence generators with which we are most concerned are those within the airplane itself, and are almost exclusively electrical devices of one sort or another. At medium frequency and high frequency almost any electrical device may act as a generator of radio influence, but at very high frequency the main source is the engine ignition system. Other devices may be troublesome, but these cases are infrequent. They will be dealt with later in this paper.

The Aircraft Ignition System

The aircraft ignition system consists essentially of a high-voltage magneto and distributor. See Figure 1 for a sketch of the system.

In the magneto itself, the primary circuit is controlled by the opening and closing of a contact switch. The secondary circuit is connected through the distributor to the appropriate spark plug. Provisions are made for starting by the use of either a booster coil or an ignition vibrator; the booster coil feeds high voltage directly into the distributor, while the ignition-vibrator type is directly connected to the magneto primary coil. In either case, operating power is supplied by the d-c power system of the airplane.

In order that the pilot may control the ignition system, a wire from the primary side of the magneto is run to a control switch in the cockpit. Here, provisions are made for grounding the primary of the magneto. The very steep fronts characterizing the voltage in the primary circuit (see Figure 1A) make it capable of impulse exciting any circuit to which it is coupled. For this reason the magneto grounding switch and the grounding leads should be well shielded. The switch should be one in which the battery switch is not included, and should be grounded to basic structure as close to the switch as possible. If the grounding wire runs outside the ignition switch box, it should be shielded also.

Since the voltage in the secondary circuit is definitely of oscillatory character (see Figure 1B), it is necessary to shield every portion of this circuit very thoroughly. The instantaneous energy content of these oscillations is very high, and the frequency range goes up into the superhigh-frequency region.

The oscillatory circuit, composed in the main of the spark plug, the spark-plug-lead capacitance and inductance, is not a simple circuit. There may be several oscillatory circuits set up, each with its own fundamental frequency. The lowest-frequency circuit will be that one utilizing the full-lead inductance and capacitance; peak currents here may go as high as 85 amperes.[1] A higher-frequency oscillatory circuit may be one formed by the spark plug itself, and peak currents here may run from 100 to 175 amperes.[2] Furthermore, there may be a local oscillatory circuit formed in the magneto and distributor housing, with peak currents here also running into high magnitudes.

It must be appreciated that the shielding of circuits containing currents of such high magnitudes is not an easy matter. The design of all mating parts must be such as to reduce the impedance to as low a value as possible; simple butt joints, pressure-held, are unsatisfactory, as they rarely permit continuous contact surface. In a conduit coupling joint, for example, it is not enough that contact be made at one or two points on the circumference of the fitting. There must be a number of contacts about the circumference, so that the radio-frequency current may have a flow path at any point, and the current density about the joint is evenly distributed.

Since the booster coil is connected directly into the distributor, and is operated by the airplane's electric system, it provides a simple method whereby the radio-frequency energy generated in the high-voltage system may be coupled into the aircraft wiring system. The ignition vibrator, on the other hand, connects to the low-voltage side. This is also a coupling path to the aircraft wiring system, but is not so effective as the one through the booster coil. In either case it is wise to place a high-frequency filter in the positive feed to these devices.

Up to now the magneto grounding leads have been treated as if they carried only the pulses which are characteristic of the low-voltage side of the ignition system. However, since these leads enter the magneto, it is inevitable that they should be affected to some extent by the radio-frequency fields set up by the secondary or high-voltage system. They do carry small radio-frequency currents from this source, but generally the amplitude of these currents is small and not nearly so effective in causing radio noise as the pulses set up by the action of the contacts.

The aircraft ignition system, therefore, may be divided into two parts as far as its radio-influence generating capacities are concerned. First, there is the low voltage or primary side of the magneto; the only coupling path for this system is through the magneto grounding leads and the ignition vibrator. Second, there is the high-voltage side or secondary side, which may radiate directly, or may couple through the booster coil. These coupling paths are the only ones by which the engine ignition system may transfer radio-frequency energy outside itself. This last statement must be considered carefully by those making radio-noise investigations. If all these coupling paths are controlled, there is no chance of radio-noise troubles. Radio-noise investigations involving the ignition system are simply problems of determining the coupling path and eliminating it. The high-voltage system may be the most difficult to control, as such high peak energies are involved, and the return paths for the radio-frequency currents are often difficult to control. Magneto and distributor construction is such as to provide possible high resistances in the current return path, such as the mating surfaces between magneto and distributor parts. However, the currents here are low in value compared with those in the spark-plug leads (except where local oscillatory circuits are formed), and are controlled much more easily. This is almost entirely a matter of good magneto and distributor design.

Other Radio-Noise Sources

As previously noted, other radio-noise sources are very few in number, particularly at very high frequency. What few there are depend upon their ability to produce a voltage surge with a steep wave-front, so almost any interrupting device may be effective. Cases have been encountered where small motors have caused trouble, even though no commutator trouble was evident. Here the motor was high speed, with consequent high-frequency commutator ripple, and was mounted upon a plywood deck where bonding was difficult. Good bonding was the answer in all cases.

Ignition systems for heaters have been developed in the past few years which are a modification of the aircraft-engine ignition system. That is, either a spark coil controlled by an interrupter and fed from the d-c power system may supply a conventional spark plug, or a transformer operating off the 400-cycle supply may operate the spark plug. In either case, a great deal of interference may result. The problems presented by this system are identical to those presented by the engine ignition system, with the added complication that the equipment is lo-

ated within the fuselage. It is necessary o shield the secondary circuit very thor-ughly, whereas the primary circuit must e well filtered.

As is well known, radio-influence ources are plentiful at medium frequency nd high frequency, and are usually mo-ors of one sort or another; dynamotors re apt to be particularly troublesome. n any event, none of the devices will ause trouble if the coupling paths once re determined and eliminated. A small mount of induction coupling is evident rom some of these devices on occasion, articularly if they are unbonded. If he leadins are run clear of the motors o trouble will be experienced in any but he exceptional case. The main coupling ath is through the aircraft wiring, and his will be dealt with later.

The aforementioned references to bond-ng must not be construed to mean that onding connectors are to be used; it is nly necessary that the motor or dy-amotor case be well grounded through he usual bolts or clips. Where the part s mounted upon a wooden deck it is sual to provide good bonding to the earest structure.

Methods of Radio-Noise Transmission

The output of a radio-influence gen-rator is of no consequence if a coupling ath does not exist between the genera-or and some impedance element in the eceiver input circuit. For some time was believed that either the antenna r the antenna leadin was always the npedance element affected and that adiation was the coupling path, but this as been disproved largely at medium nd high frequency. At these frequen-ies, the antenna is almost never affected y radio-influence sources within the air-lane. The coupling path is almost in-ariably the aircraft wiring system, either y coupling to the leadin or common im-edance coupling which will be discussed ter.

At very high frequency the situation is omewhat different; direct coupling may ccur to the engine ignition system, and he antenna will be the impedance ele-ent affected. There are, however, ther coupling paths at these frequencies, nd it is the purpose of this section to dis-uss these paths.

Before proceeding with the discussion must be pointed out that the conven-onal methods of measuring radio-in-uence sources are in error, in that only he voltage is measured. The voltage easured is a function of the internal npedance of the source, the impedance f the external circuit to which it is con-ected, and the open-circuit voltage of he source, and consequently will change apidly with any change in the output-rcuit impedance. Resonance effects so enter, as the aircraft wiring system must be considered as an impedance network. It is therefore possible to have high voltage and low current at the radio-influence source output, and low voltage and high current at some point in the network, and vice versa. In any event, since either electromagnetic, electrostatic, radiation, or any combination of these couplings may occur, and both the current and the voltage at the impedance element through which coupling occurs must be taken into account, voltage measurements alone are insufficient.

The Aircraft Wiring System—Unshielded

As might be expected, the aircraft wiring system exhibits transmission-line characteristics. To determine the attenuation that might be encountered in a section of aircraft wiring, a series of tests was made on a typical aircraft open wiring bundle consisting of 15 wires. The results of this test are shown in Figure 2, where the attenuation of the aircraft wiring is compared with the attenuation of conventional coaxial cables. For the aircraft wiring the measurements were made between one wire and structure ground. It will be noted that, while the attenuation of aircraft wiring is considerably higher than for a good twin-conductor coaxial cable, it was only slightly higher than for a rubber-insulated twisted pair. The curves shown would be entirely valid if aircraft wiring consisted of simple runs from one point to another, but it does not. The aircraft wiring bundle may contain a number of cables, terminating in lights, switches, or other devices, and an analysis of the aircraft wiring system on the basis of a transmission line would not be very useful. It is preferable to consider it on the basis of an impedance network, as previously noted. On that basis the voltage and current relationships in any network element are a function of the impedance, the voltage and current, and the mutual inductance relationships of all the other elements, plus the generator voltage and internal impedance. Even an approximate calculation of the relationships would be very complex, this consideration being presented simply to indicate the type of approach necessary. An approach by means of voltage measurements alone is inherently faulty.

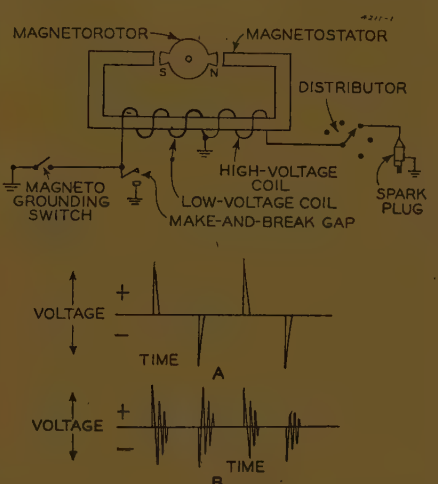


Figure 1. Aircraft ignition system

Magnetorotor, distributor, and make-and-break gap. All are engine-driven and set to produce spark at proper moment in engine cycle

A. Voltage on magneto grounding wire, peak about 200 volts

B. Voltage in secondary circuit, peak depending on pressure, gap spacing, and so forth—about 15,000 volts

It is evident that aircraft open wiring of itself cannot be depended upon for any large attenuation, except in rather long runs. It is also evident that aircraft open wiring will act precisely like any other transmission line used to couple a generator to a terminal impedance. If there is a mismatch, reflections will occur with the formation of standing waves upon the line. Since one or more wires in a bundle may be carrying a wave from the same radio-influence source, but fed over a slightly different path, the standing waves on one wire will not agree in location with the standing waves on other wires. The result of this will be that any attempt to locate standing waves with a noisemeter will be quite confusing, especially at the higher frequencies.

It is probable that this has contributed to the many attempts at source suppression; the whole fuselage area seems full of "noise" and source filtering does eliminate it. The factors which make source filtering impractical have been reviewed in other literature, however, and will not be taken up here.[3]

The Aircraft Wiring System—Shielded

A discussion of the portion of the aircraft wiring that is still shielded may seem academic, as there is so little of it left. Nevertheless, the portions that remain shielded are quite important and deserve attention. In the main, these portions are the ignition system, particularly the magneto grounding leads.

The shielded wiring also may be considered as a transmission line. The attenuation here is somewhat greater than for open wiring, since a great portion of the area of the conduit is filled with a dielectric of relatively high loss at high frequencies. Current flows on the conductors inside the conduit and upon the inner surface of the conduit exactly as in a coaxial cable, the resultant field being confined within the conduit for all practical purposes.

A conduit, either solid or flexible, may contain a joint at some place in the line, and such joints easily may be of rather high resistance, if the joint is not well made. If this local area of high imped-

ance is located where the current density is high, the current will seek a better path, in many instances flowing entirely outside the conduit. This area of high current density outside the conduit will create a local field and may excite conduit which is some appropriate multiple of one-quarter wave. If this field is set up in a section that is not well shielded or near one of the openings in the fuselage, radiation may take place outside the fuselage, and the antenna will be affected directly.

It must be emphasized here that the field produced by current flow outside the conduit is not the same field that is found within the conduit and which is created by interaction between the current flowing on the conductors and on the inner surface of the conduit. That is, it must not be conceived that the field within the conduit penetrates through the conduit in some manner; nothing of the sort occurs. A better conception is that the field within the conduit is the power-transmission medium, and the currents that flow are set up by the field and are a source of losses. When these currents flow outside the conduit a local field is created there, incidentally introducing another loss.

Radiation is most likely to take place from the engine-section ignition system, which in most cases is enclosed in flexible conduit. The leads to the spark plugs are very high-frequency oscillatory circuits, as previously noted, and the current density at joints or glands may be extremely high. A poor joint in this area cannot be tolerated; the field set up about the joint is of such intensity that the engine cowling is insufficient to attenuate it, and direct coupling to the antenna results.

Flexible conduit is often suspected of radiation, since it is almost invariably possible to detect a small field about it. This is the result of the in-and-out weave of the outer sheath, a small portion of the current flowing upon the inner surface of the conduit being conducted to the outside surface, setting up a small field there. The field noticed is the resultant of all the fields set up by the tiny currents flowing outside the conduit.

The field about flexible conduit is commonly known as a "leak," the inference being that radio waves or even the field between the inner and outer conductors actually leaks out between the wires composing the weave. The term "leak" is unfortunate, as it does not describe adequately what is taking place. The same term, also used incorrectly, is applied to describe the fields set up by current flows at the mating surfaces between the magneto and the distributor, or between the magneto and the engine. A slight consideration will show that the spaces where such "leaks" are believed to take place are so much smaller than the length of the wave

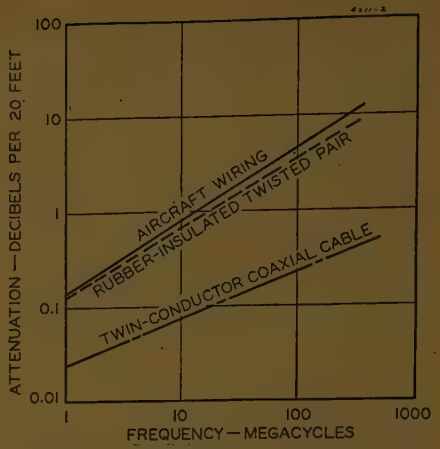


Figure 2. Attenuation of various types of transmission lines

in question that actual leakage cannot take place. The remedy ordinarily prescribed to cure this "leak," that of grinding the mating surfaces so that contact will be uniform, simply provides a low-impedance path for the current, so that it is not forced to seek a path which lies outside the magneto.

Common Impedance Coupling

Mention has been made previously as to the introduction of radio noise into a receiver by the formation of a radio-influence voltage across the impedance between the receiver chassis and ground, the impedance being directly in the antenna circuit of the receiver. Such cases are more common than is realized, and are more responsible for radio noise than is ordinarily believed. Inasmuch as any such voltage will of necessity be small, the only manner in which the radio receiver can be affected is by the inclusion of one of these impedance elements in the antenna circuit, and no other circuit than the antenna need be considered.

Medium and High Frequencies

These frequencies will be discussed in some detail before proceeding to the very high-frequency case, as both have many points in common, and the very high-frequency case is more easily understood after reference has been made to the medium- and high-frequency cases.

With reference to Figure 3, it will be noticed that radio-influence currents entering the receiver over any of the possible paths (power wiring, control wiring, and so forth) must flow to structure through the ground impedance, which is composed of the bonding connector, and any other impedance path present, all being in parallel. This impedance is of a very complex nature and may be either capacitative or inductive, depending upon the installation and the frequency. This is because the impedance of a bonding connector goes up rapidly with frequency, while the reactance offered by the capacitance between the chassis and structure decreases with frequency. In any event, a complex voltage is formed across the impedance, and an inspection of the block diagram will show that this voltage is directly in series with the antenna circuit. For all intents and purposes, then, there are two generators in the antenna circuit. First, the equivalent signal generator, which has an internal impedance equal to the antenna impedance and an output voltage equal to the signal induced in the antenna by the electromagnetic waves from the station being received. Second, the equivalent radio-influence source, which has an internal impedance equal to the ground impedance and a voltage equal to the product of the radio-influence current through it and the ground impedance. This is shown in Figure 3. The voltage which appears across the input circuit of the receiver is the resultant of the vector addition of the signal and radio-influence currents times the impedance of the input circuit. Since signal voltages are apt to be low, it will not require much current flowing through the bonding impedance to provide a voltage at least equal to the signal voltage and probably much larger.

It is apparent that the installation of coaxial cable or twisted pairs in the leadin on many installations other than on aircraft are effective in controlling "noise," not because induced "noise" voltages are balanced out, but because common impedance coupling is eliminated. It may be very difficult to make a separation test that will differentiate between coupling to the leadin and common impedance coupling, except in an all-metal airplane where the leadin is adequately shielded.

Three conditions must be observed in any separation test to determine the effect of common ground-impedance coupling. These are:

1. Exclude from the antenna circuit that portion which is exposed to direct coupling; that is, the antenna itself.

2. Retain within the antenna circuit all current-flow paths that are present in the normal circuit; that is, the ground connections at the receiver and the antenna mast should remain intact.

3. Replace the antenna with an equivalent impedance. At medium frequency and high frequency this may consist of a capacitor approximately equivalent to the capacitance of the antenna to the fuselage; at very high frequency the impedance should approximate that of the antenna at the frequencies being considered.

All conditions must be observed, or else the separation test will be invalid. The first condition is more or less obvious, but not the second and third. However, it must be remembered that, if the antenna-circuit-impedance relations are changed, the various portions of the test cannot be correlated, and the test results will be meaningless.

Very High Frequency and Ultrahigh Frequency

Very high-frequency and ultrahigh-frequency receivers are invariably coupled to the antenna by some form of coaxial cable, with the outer sheath ordinarily of a flexible weave to facilitate installation and maintenance. At the receiver and at the antenna this cable terminates in a special fitting, so that it may be disconnected readily, again to facilitate installation and maintenance. Each of these breaks in the coaxial cable run introduces an additional impedance, however slight. The greatest source of extraneous impedance elements are these fittings, especially where the antenna circuits have not been designed with the elimination of radio noise in mind. A typical example, using a current very high-frequency transmitter–receiver, will be given.

The coaxial fitting is on top of the transmitter–receiver (an error to begin with, as any currents induced in the coaxial cable sheath have a relatively long path to travel to ground and can get into all sorts of mischief along the way); the center conductor is brought down to the antenna switching relay, where, by means of long leaf-type spring contacts, connection is made either to the receiver or transmitter. The connection to the receiver input coil is well routed, but the input coil is grounded directly to the chassis, forcing the antenna signal currents to travel back to the coaxial fitting over the best path possible. The coaxial cable fitting is grounded to the chassis by means of the mounting bolts and a copper lead, but is held about an inch off the main chassis by the mounting bolts, so that the sheet-metal-case cover is flush with the base of the coaxial fitting.

Several impedance elements are apparent here: the impedance of the fitting itself, the impedance between the fitting and the chassis, and the impedance between the ground point of the coaxial fitting and the receiver input coil. Obviously, the total impedance between the end of the coaxial cable and the ground point of the receiver input coil will raise the fitting above ground potential, as far as the receiver is concerned. It has been observed that, with the antenna terminal shielded, touching or even bringing the finger close to the coaxial fitting will bring the receiver up to full output level, if even a weak field is present.

On this same transmitter–receiver the coaxial cable fitting to the antenna mast introduces another long lead from the fitting to the ground stud on the mast. Ordinarily not too much attention is paid to the ground connection between this stud and structure, so this may also be of high impedance, particularly if ordinary bonding braid is used. The bonding connector here should be as short as possible and preferably of copper strap.

Ordinary flexible shielding, such as is used on shielded leads on interphone systems, has been shown to have an attenuation of 60 decibels, and it is very unlikely that fields are present inside the airplane fuselage of sufficient strength to induce voltages in the inner conductor of a coaxial cable. There are, however, fields due to standing waves present upon the aircraft wiring, and wherever wiring and coaxial cables run in parallel, it is a very simple matter to set up a current flowing along the outer surface of the sheath.

The interference that may result from current flow in coaxial cable sheaths has been investigated thoroughly. A test setup of the receiver previously mentioned was made substantially as follows: all control and power wiring to the receiver was isolated and shielded by copper sheeting, all joints being well soldered, and all possible coupling to the receiver from this path eliminated. A coaxial cable with solid dielectric insulation, standard sheath weave and vinyl covering was extended about 15 feet away, the free end suitably terminated in the characteristic impedance of the cable with the terminal impedance also shielded. A very high-frequency signal generator was set up with an insulated coupling lead two feet long taped to the coaxial cable. With a fixed output of the signal generator the receiver output level was noted before and after the coaxial cable fitting was bonded to the chassis.

The results of this test in terms of decibels rise above the background level (reference level, one milliwatt in a 600-ohm line) were 24-decibel rise with the coaxial fitting unbonded; two-decibel rise when bonded.

It thus was concluded that common impedance coupling was responsible for the increases in output level. To test the accuracy of this conclusion, the antenna connector of the receiver was terminated at the coaxial fitting and shielded so that no coupling could occur; then the sheath of the coaxial cable was attached to the fitting. Coupling to the coaxial cable was accomplished as before, and it was found that, with approximately the same signal level from the signal generator as before, the output level of the receiver was 27 decibels above the background level. The installation of a bond on the coaxial fitting caused a decrease in output level of 21 decibels.

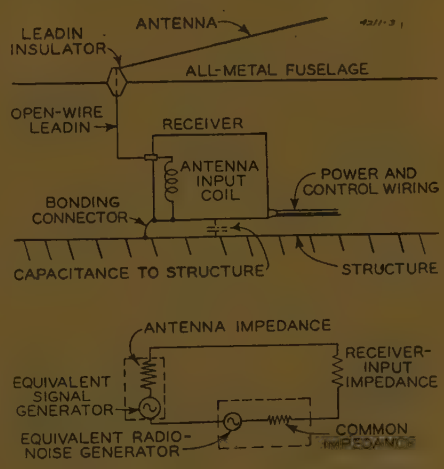


Figure 3. Common impedance coupling

A. Block diagram of typical radio-receiver installation at medium and high frequency

B. Equivalent circuit diagram usable for any frequency range

To summarize, it is evident that a receiver, although fitted with a coaxial cable, may be affected by currents in the coaxial cable sheath. This is accomplished by the currents flowing through a relatively high impedance common to the antenna circuit of the receiver. Such an impedance may be one formed at any portion of the antenna circuit by a discontinuity, or in any portion of the circuit in which both the antenna current and the radio-influence currents flow over the same path.

Elimination of Very High-Frequency Radio Noise

The proper starting point in the elimination of radio noise at any frequency is in the design of the radio receiver. Unfortunately, the designer usually is concerned mostly with circuit design, the antenna being only the means of introducing a signal of certain characteristics, so that something else happens at the output end of the receiver. Bench testing is done under conditions which do not approximate those in service with the result that a receiver which performs perfectly under test may be noisy when installed in the airplane. Then in an effort to eliminate the interference various methods of interference suppression are used, including filters upon all items of suspected electric equipment, bonding, and shielding.

Interference suppression by the means enumerated has become a sort of obsession, so much so that suppression methods are applied by specification requirements to all equipment, whether or not they produce radio noise. In view of the demonstrated fact that much of this interference could be eliminated by proper design of the radio receiver, it is at once apparent that the suppression devices added are largely dead weight, decreasing the pay or bomb load, as the case may be.

Lest the whole burden of the blame be shifted too quickly to the radio-equipment designer, it should be said here that the blame rests equally upon him, the designer of the electrical equipment producing the interference, and the designer of the radio installation in the airplane. A very great deal can be done by the equipment designer, for, while we know of no way as yet to correlate the radio-influence voltage of a device with the radio noise this device produces in

the receiver, we do know that a motor can be designed to create a minimum of radio-influence voltage. The electrical-equipment designer must have not only the performance of his design in mind; he must consider its effect upon other equipment. The extra weight in filters, and so forth, which is necessary to make one item of equipment comparable in all respects to another item is directly chargeable to that item.

The designer of a radio installation may destroy entirely the good work of the radio-equipment designer and the electrical-equipment designer. Long antenna leadins are sometimes unavoidable, but it is inexcusable to place wiring or rotating equipment in close proximity to that leadin without taking adequate precautions so that there will be no interference. But inasmuch as the radio-installation designer is confronted with the problem of making presently available equipment function properly, it is necessary that this designer know the methods which are practical and workable for the reduction and elimination of radio noise.

To this end, in the section to follow, the general outlines of a test program are given, specifically for very high-frequency radio-noise tests. Before any such test is made the test man must make himself thoroughly familiar with the airplane upon which he is working insofar as the electric and ignition system are concerned. All ignition-system wiring should be traced out, both on the wiring diagram and on the airplane itself. The installation of the radio equipment should receive the most careful consideration, the feed points of the power supply, the location of the control equipment and wiring to it, and the run of the coaxial cable from the antenna to the receiver; all should be well fixed in mind. After this, the test man should determine the points at which the separation tests are to be made. Finally, he should set down in a logical sequence the tests he intends to make, and this sequence should be followed at all costs during the tests. This latter is especially important, as it is very easy to allow oneself to be distracted during the tests. The result is that invariably the most important test is left out, and a guess will have to be made at the results that should be on hand.

Test equipment need not be elaborate. There should be prepared beforehand a list of needed equipment, which of course will be dictated to some extent by the available equipment and the experience of the test man. A complete listing will not be given here, but three items must be considered:

1. *Antenna termination.* This should be a shielded box, containing an impedance approximately equal to that of the antenna it is to replace with a fitting, so that the coaxial cable can be attached, and another fitting, so that the box may be attached to where the coaxial cable normally goes. This is important, as it preserves the continuity of the ground circuit of the coaxial cable sheath.

2. *Shielded oscillator.* This oscillator should be fitted so that it can be attached to the magneto pencils or one of the spark plugs. In this way, tests can be made upon the effect of the magneto grounding wires, or loose coupling and gland nuts may be found while the engine is not running.

3. *Duplicate receiver.* The radio receiver as mounted in the airplane may be surrounded by intense fields created by wiring carrying radio-influence currents, and probe tests made with this receiver may be misleading. It is desirable to have a duplicate receiver for such tests, complete with power supply. If the receiver and power supply are mounted on a metal panel and placed on a dolly, they may be moved as convenience dictates and will prove very useful.

No attempts should be made to interpret the results of the probe test in terms of absolute field strength; this is virtually impossible. Any comparisons made should be on the basis of a qualitative analysis only. Field-strength measurement at any frequency is a laboratory test; this is especially true at very high frequency.

Very High-Frequency Radio-Noise Test Analysis

Inasmuch as the main radio-influence source at very high frequency is the engine ignition system, this source alone will be considered here. As the preceding text has indicated, the ignition system may be broken down conveniently into two parts: the high-voltage and low-voltage sides. These will be taken up separately, and the various coupling paths will be considered.

Coupling Paths from the Aircraft-Engine Ignition System

Low-Voltage System

(*a*). *Radiation Coupling to the Antenna.* This may occur only where the low-voltage system impulse excites an impedance element which may couple directly to the antenna. This can be done by:

1. Magneto grounding leads. If there is a poor joint at any place along the conduit carrying these leads, or any coupling between these leads and the aircraft wiring at the grounding switch, an impedance element may be impulse-excited. If there is a coupling path between this impedance element and the antenna, interference will result.

2. Ignition vibrator. Since this is fed from the aircraft d-c power system, the aircraft wiring may be impulse-excited. This will result in interference if a coupling path is present between any impedance element of the wiring and the antenna.

(*b*). *Conduction Coupling.* This occurs where currents are induced by any means whatsoever in any portion of the antenna system other than the antenna itself. It may be caused by the magneto grounding leads or the ignition-vibrator feed wire impulse-exciting any portion of the aircraft wiring system.

High-Voltage System

(*a*). *Radiation Coupling to the Antenna.*

1. Direct coupling. This may occur where any portion of the high-voltage ignition system is in such condition as to set up a local field which couples directly to the antenna.

2. Coupling through some other impedance element. Radio-frequency currents fed through any path, either the magneto grounding leads or the booster coil leads, may excite some impedance element from which there is a coupling path to the antenna.

(*b*). *Conduction Coupling.* This is analogous to conduction coupling in the low-voltage system and occurs where radio-frequency energy is coupled into the aircraft wiring system from the magneto grounding leads or the booster coil, inducing a current into some impedance element of the antenna circuit.

The breakdown given may seem oversimplified, but it is all there. It is important to realize that there are only a few coupling paths from the aircraft-engine ignition system, and then plan the test procedure to isolate them one at a time.

As a basic test procedure, the following is suggested: Pull the magneto grounding pencils and then disconnect the leads to the booster or ignition vibrator. If radio noise still persists, stop the engines and feed a signal from the oscillator into one of the spark-plug leads. A probe test will show which coupling is loose; often it is necessary only to short-circuit across the suspected connections with a screw driver to check the effectiveness of the electrical path through them.

Next, with the ignition harness cleaned up, run the engines with the booster or ignition coil connected and the magneto grounding pencils disconnected, then vice versa.

This procedure definitely will indicate the coupling paths; it then will devolve upon the test man to determine the circuit elements through which coupling is occurring.

References

1. Electrical Character of Spark Discharge of Automotive Systems, M. F. Peters. National Bureau of Standards, *RP*1032.

2. Precipitation Static, Radio-Interference Phenomena Origination on Aircraft, E. C. Starr. Engineering Experimental Station, Oregon State College, *Bulletin*, number 10, June 1939.

3. Radio-Noise Elimination in All-Metal Aircraft, Fred Foulon. AIEE Transactions, volume 62, 1943, pages 877–91.

4. Methods of Measuring Radio Noise. Report on radio reception of Edison Electric Institute, National Electrical Manufacturers Association, and Radio Manufacturers Association. Edison Electric Institute publication *C*9, December 1935, New York, and *G*9, February 1940.

Altitude Rating of Electric Apparatus

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THE proper functioning of electric equipment is of prime importance in the operation of the military and naval airplane of today. The wide use of such equipment and the continual increase in the operating altitude of aircraft make it essential that the factors affecting the operation of electric apparatus at high altitudes be investigated if the apparatus is to be designed properly for this new and expanding application.

This paper studies the effect of altitude on the ratings of rotating electric machines and, after determining the fundamental principles involved, discusses these in relation to the application of such machines in modern aircraft. It is shown that the rating of a self-ventilated direct engine-driven aircraft generator decreases rapidly with altitude and, at some altitude within the operating range of the airplane, the generator may be able to dissipate only its no-load losses. It is shown also that an air-scoop-ventilated direct engine-driven aircraft generator maintains its rating over most of the operating range of present-day aircraft. Finally, certain sea-level tests are proposed from which calculations of rating under altitude conditions can be made.

The theory and tests presented in this paper have been checked by altitude-chamber studies on an aircraft-type d-c generator.

Fundamental Analysis

Heat Transfer in Electric Machines

One of the principal factors determining the rating of electric apparatus is the maximum temperature to which its insulation can be subjected without failure during the required life of the machine. This operating temperature is a function of the losses of the machine and its ability to dissipate the resultant heat.

An electric machine may dissipate its losses by four means:

1. Forced convection between winding and core surfaces and cooling air.
2. Free convection between frame and ambient air.
3. Radiation between frame and surrounding objects.
4. Conduction between frame and mounting.

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The watts loss which may be dissipated by these processes is a function of the temperature difference between the machine surfaces and the ambient temperatures. The machine temperature rises until the total watts loss dissipated just equals the input losses; that is, until equilibrium is reached. Thus, any increase in load (and hence, loss) will increase the machine temperature unless the additional heat can be removed from the machine by an increased heat transfer. Conversely, any reduction in heat transfer will increase the machine temperature unless the loss is reduced.

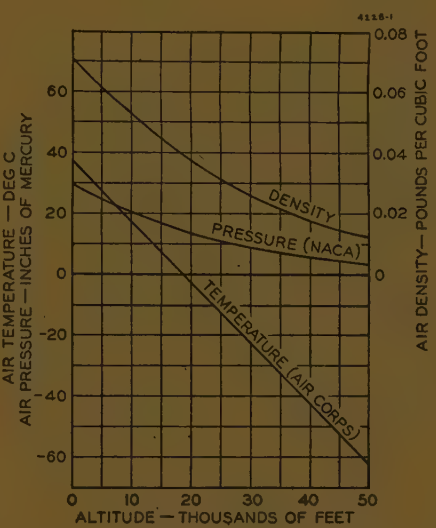


Figure 1. Properties of atmospheric air

The heat transfer between the machine and the ambient air also is affected by the air density. Thus, at the reduced densities and temperatures encountered at altitude (Figure 1), the watts loss which can be dissipated by an electric machine may change, and its rating at altitudes may be different from that at sea level.

Each of the heat-transfer processes is now considered individually, and its variation with density and temperature determined.

Forced Convection. The transfer of heat by the forced circulation of a gas or liquid is referred to as heat transmission by forced convection. In an electric machine the cooling air enters the machine at an ambient temperature Θ_{a1}, and in wiping the windings and core removes heat from these parts. The resultant increase in air temperature ("air rise") is denoted by Θ_{ar}. This process is shown graphically in Figure 2, where Θ_{s1} is the surface temperature of the windings or core of the machine and Θ_d is the temperature drop between the hot surface and the cooling medium ("surface drop").

The maximum allowable winding surface temperature is determined by the insulation used in the machine. Thus, if Θ_{s1}', Θ_d', and Θ_{ar}' denote values of surface temperature, surface drop, and air rise, respectively, at the point at which the ventilating air leaves the machine,

$$\Theta_{s1}' = \Theta_{a1}' + \Theta_{ar}' + \Theta_d' \text{ degrees centigrade} \quad (1)$$

Θ_{ar}' is a function of the weight flow of air through the machine and the watts loss taken away by this air. Thus,

$$\Theta_{ar}' = \frac{0.133\, q_{for}}{W} \quad (2)$$

where

q_{for} = watts loss dissipated by forced convection
W = weight flow of cooling air through machine, pounds per minute
$W = \rho Q$
ρ = density of cooling air at entrance to machine, pounds per cubic foot
Q = volume flow of cooling air at entrance to machine, cubic feet per minute

The surface drop at the exit of the machine is:

$$\Theta_d' = \frac{q_{for}}{C} \text{ degrees centigrade} \quad (3)$$

where

C = over-all thermal conductance between machine surface and cooling air, watts per degree centigrade

The thermal conductance C is a function of the density of the cooling air ρ, its velocity v past the surface being cooled, and the area of that surface A_1. It has been shown (reference 1) that for the turbulent flow of air in ducts

$$C \propto A_1(\rho v)^{\alpha} \quad (4)$$

In an electric machine the velocity v is composed of two components: the axial velocity of the cooling air through the ducts of the machine v_a, and the peripheral velocity of the rotor v_p. Tests on d-c aircraft-type generators indicate that for ratios of axial to peripheral velocity approximately less than 2, the axial velocity can be neglected; whereas, for ratios greater than 2, the peripheral velocity can be neglected. In the practical case of self-ventilated and separately ventilated aircraft generators, the former type usually has a ratio of axial to peripheral velocity well below 2, and the latter type has a ratio well above 2. Hence, the transition range need not be studied closely in the present investigation.

The axial velocity v_a is:

$$v_a = \frac{144Q}{A_{\text{ll}}} \text{ feet per minute} \quad (5)$$

where

A_d = total area of ducts through which the cooling air flows, square inches

Thus, from equations 4 and 5 and the afore-mentioned assumptions,

$$C_{self} \propto A_1 v_p^{\alpha} \rho^{\alpha} = \frac{1}{k_1} \rho^{\alpha} \quad (6)$$

$$C_{sep} \propto A_1 \left(\rho \frac{144Q}{A_d}\right)^{\alpha} \propto A_1 \left(\frac{144}{A_d}\right)^{\alpha} W^{\alpha} = \frac{1}{k_2} W^{\alpha} \quad (7)$$

where k_1 and k_2 are constants for any given machine operating at constant speed, and C_{self} and C_{sep} are the over-all thermal conductances for self-ventilated

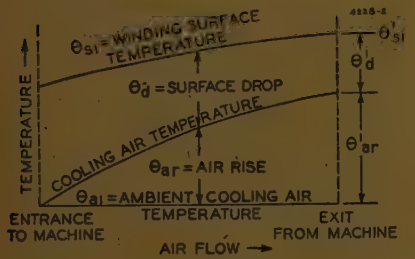


Figure 2. Temperature–air-flow diagram of a single-end axially ventilated electric machine

and separately ventilated machines, respectively.

In smooth ducts, $\alpha = 0.8$; in rough ducts, α may vary from 0.5 to 1.0, depending on the degree of roughness. On one aircraft-type d-c generator the use of an exponent of 0.5 checked test data.

Substitution of equations 2, 3, 6, and 7 into equation 1 gives:

Self-ventilated:

$$q_{for} = \frac{\theta_{s1}' - \theta_{a1}}{\frac{0.133}{W} + \frac{k_1}{\rho^{\alpha}}} \text{ watts} \quad (8)$$

Separately ventilated:

$$q_{for} = \frac{\theta_{s1}' - \theta_{a1}}{\frac{0.133}{W} + \frac{k_2}{W^{\alpha}}} \text{ watts} \quad (9)$$

Hence, if θ_{s1}', θ_{a1}, ρ, and W are known at a given altitude, the watts loss which can be removed by forced convection at that altitude can be found if k_1, k_2, and α are known. These constants can be determined from sea-level tests on the machine.

Free Convection. The transfer of heat by the natural circulation of a gas, caused by a nonuniform temperature (and hence density) variation within the gas, is referred to as heat transmission by free convection.

The heat transferred from a horizontal cylinder in air is

$$q_{free} = 1.83 \times 10^{-4} A_2 p^{0.5} \frac{(\theta_{s2} - \theta_{a2})^{5/4}}{L^{1/4}} \text{ watts} \quad (10)$$

where

q_{free} = watts loss by free convection

A_2 = area of surface subject to free convection, square inches

p = atmospheric pressure, inches of mercury

θ_{s2} = convecting surface temperature, degrees centigrade

θ_{a2} = ambient air temperature for free convection, degrees centigrade

$L = \frac{L_h L_v}{L_h + L_v}$, feet

L_h = horizontal length of cylinder, feet

L_v = vertical height of cylinder, feet

The watts dissipated by free convection at any altitude therefore can be calculated from equation 10, since all factors are known. The surface temperature can be determined from sea-level tests. Tests taken under actual altitude conditions have indicated that the frame temperature remains essentially constant at various altitudes for the same maximum internal surface temperature, unless there are large changes in the heat dissipated from the frame of the machine.

It should be noted that the watts dissipated by free convection vary as the 0.5 power of the atmospheric pressure.

Radiation. Radiation is the method of heat transmission by which heat energy is transferred from one body to another through a transparent medium with no change in the temperature of the medium. When the area of the radiating body is small compared with that of its surroundings, the watts dissipated by radiation are:

$$q_R = 0.00375 \epsilon A_3 \left[\left(\frac{\theta_{s3}}{100}\right)^4 - \left(\frac{\theta_{a3}}{100}\right)^4\right] \text{ watts} \quad (11)$$

where

q_R = watts dissipated by radiation

ϵ = emissivity of the surface of the radiating body (0.9 for a body covered with a nonmetallic paint)

A_3 = area of radiating surface, square inches

θ_{s3} = radiating surface temperature, degrees centigrade absolute

θ_{a3} = ambient air temperature or temperature of surrounding bodies (if close enough) for radiation, degrees centigrade absolute

The preceding equation shows that the watts dissipated by radiation are independent of air density or pressure and, hence, of altitude for constant θ_{s3} and θ_{a3}. The same statements concerning these temperatures apply to this case as to the case of free convection.

Conduction. The heat transferred between two bodies by the conduction process is directly proportional to the conductivity of the transmitting medium, its area, and the temperature difference between the two bodies, and inversely proportional to the length of path. In an electric machine heat is conducted through its mounting, the direction of the transfer depending on the relative temperature of the two bodies. This heat transfer is difficult to calculate, since the length of path and the temperature difference are not accurately known. However, it is not affected by air density or temperature, provided the temperature of machine and mounting remain constant. Measurements of these temperatures must be made to determine the direction of heat flow, and the results obtained used to temper the calculations of total heat transferred by the other three processes. If heat is removed from the machine it can be neglected and used as a "safety factor;" if it is added, an estimate of the quantity added may be necessary.

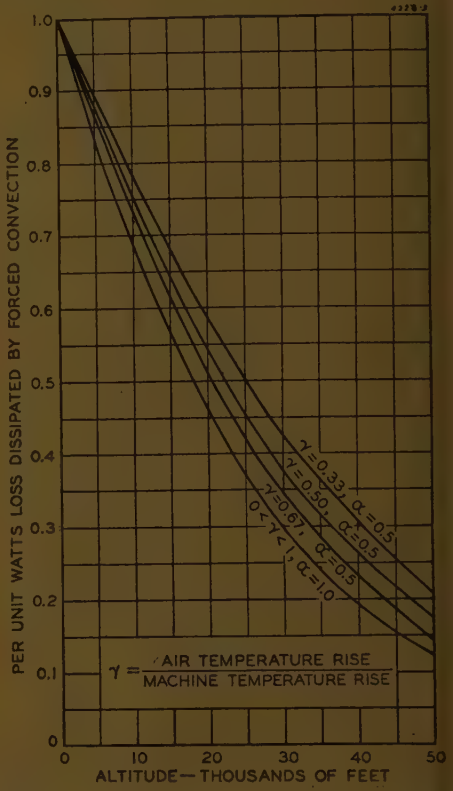


Figure 3. Effect of altitude on the watts loss dissipated by forced convection from a self-ventilated direct engine-driven aircraft generator

Constant maximum generator-winding surface temperature—140 degrees centigrade
Constant engine-accessory-compartment temperature—38 degrees centigrade

SUMMARY OF HEAT-TRANSFER EQUATIONS

$$q_{tot} = q_{for} + q_{free} + q_R \text{ watts} \quad (12)$$

Self-ventilated:

$$q_{for} = \frac{\theta_{s1}' - \theta_{a1}}{\frac{0.133}{W} + \frac{k_1}{\rho^{\alpha}}} \text{ watts} \quad (8)$$

Separately ventilated:

$$q_{for} = \frac{\theta_{s1}' - \theta_{a1}}{\frac{0.133}{W} + \frac{k_2}{W^{\alpha}}} \text{ watts} \quad (9)$$

$$q_{free} = 1.83 \times 10^{-4} A_2 p^{0.5} \frac{(\Theta_{a2} - \Theta_{a2})^{5/4}}{L^{1/4}} \text{ watts} \quad (10)$$

$$q_R = 0.00375 \epsilon A_2 \left[\left(\frac{\Theta_{a3}}{100} \right)^4 - \left(\frac{\Theta_{a2}}{100} \right)^4 \right] \text{ watts} \quad (11)$$

Air Flow in Electric Machines

All the terms in the heat-transfer equations have been discussed, except the weight flow of the air used to cool the machine W. This quantity varies with altitude in a manner which depends on the type of ventilation employed. In the case of aircraft equipment, two types of machine are used: the enclosed self-ventilated machine, where the cooling air is circulated by a fan integral with the machine; and the enclosed separately ventilated machine, where the cooling air is picked up by an air scoop on the airplane and forced through the machine by the ramming pressure obtained from the airplane's motion. Single-end axially ventilated machines are discussed here, as this is the type in most common use on the equipment considered in this paper.

When the friction pressure drop of the air in flowing through the passages of a machine is neglected (a good assumption for small machines), the equation governing the air flow through a machine is:

$$H = \frac{\rho}{5.2} \left[\beta_m \frac{v_m^2}{2g} + \beta_n \frac{v_n^2}{2g} + \dots \right] \text{ inches of water*} \quad (13)$$

where

H = pressure head available for forcing cooling air through machine, measured at entrance to machine, inches of water

$\beta_m \frac{v_m^2}{2g}, \beta_n \frac{v_n^2}{2g}, \dots$ = loss in pressure at contractions, expansions, bends, and so forth, of the air-flow path, measured in velocity heads of air

Since

$$v_m, v_n, \dots = \frac{144Q}{A_m}, \frac{144Q}{A_n}, \dots$$

$$H = \frac{Q^2 \rho}{58} \left[\frac{\beta_m}{A_m^2} + \frac{\beta_n}{A_n^2} + \dots \right] \quad (14)$$

where

$A_m, A_n, \dots$ = areas at changes in path section, square inches

The bracketed expression is a constant for any machine and may be called an "air resistance," R. Hence,

$$Q = \sqrt{\frac{58H}{\rho R}}, \text{ cubic feet per minute} \quad (15)$$

*This equation assumes that the air density is constant throughout the machine. Because of the air temperature rise this is not strictly true. However, the simplicity obtained by assuming a constant density warrants its use. For more accurate calculations an average air density may be used rather than the value at the machine entrance.

The weight flow of cooling air, therefore, is:

$$W = \rho Q$$

$$W = \sqrt{\frac{58H\rho}{R}}, \text{ pounds per minute} \quad (16)$$

This equation defines the weight of cooling air flowing through the machine for a given pressure head H. This pressure head will vary with altitude, depending on whether the machine is self-ventilated or separately ventilated by the methods discussed previously.

The pressure head developed by a fan revolving at constant speed varies directly with the air density. Therefore, in a self-ventilated machine the weight flow of air is directly proportional to the air density (equation 16).

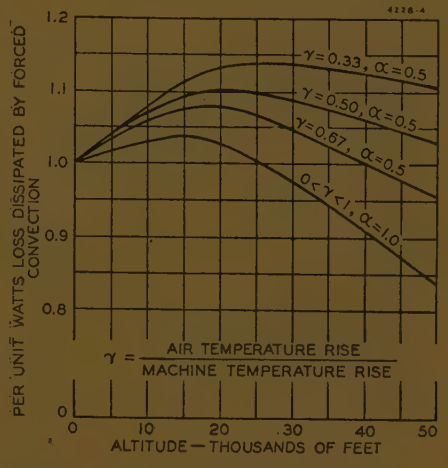


Figure 4. Effect of altitude on the watts loss dissipated by forced convection from a separately ventilated direct engine-driven aircraft generator

Constant maximum generator-winding surface temperature—140 degrees centigrade
Constant ramming-head cooling-air pressure
Cooling-air properties taken from Figure 1

For a separately ventilated machine employing an air scoop, the head H will depend on the airplane's speed–altitude characteristics. For a constant angle of attack the lift on an airplane wing is proportional to ρV^2, where ρ is the air density, and V is the velocity of the airplane relative to the air. Since a certain minimum lift is necessary to support the airplane, the minimum ρV^2 must remain constant, regardless of altitude. However, the pressure head in inches of water developed in the air scoop is directly proportional to ρV^2 (neglecting losses); hence the minimum air-scoop pressure head is independent of altitude for a separately ventilated generator. From equation 16 it can be seen that the minimum weight flow of air is thus proportional to the square root of the air density and therefore decreases with altitude, but not so rapidly as in the self-ventilated machine, where the decrease was directly proportional to the air density.

Application of Fundamental Analysis to the Determination of the Altitude Ratings of Rotating Electric Machines in Aircraft

The fundamental equations for heat transfer in rotating machines will be used now to determine the rating of electric machines under varying altitude conditions. The specific cases of self-ventilated and separately ventilated direct engine-driven d-c generators will be chosen for this discussion, each generator being considered separately. These generators are overhung from the back end of the aircraft engine. The self-ventilated generator secures its cooling air from the space back of the engine, known as the engine-accessory compartment; and the separately ventilated machine secures its cooling air from the atmosphere external to the airplane.

The general approach to the problem of altitude ratings is:

1. The determination of the ambient-temperature and air-flow conditions for each type of generator.

2. The calculation of the total watts loss which can be removed by the heat-transfer processes already discussed (heat transfer by conduction will be neglected) at the altitude at which the rating is desired.

3. The determination of the current, and hence the rating of the machine, corresponding to the total watts loss found in step 2 from a curve of watts loss of the machine as a function of its line current.

Enclosed Self-Ventilated Generator

Ambient Temperatures. The ambient temperature of the cooling air Θ_{a1} of a self-ventilated generator is that of the engine-accessory compartment. Tests have shown that this temperature is independent of altitude. Present information indicates that a minimum compartment temperature of 38 degrees centigrade and a maximum of 65 degrees centigrade may be expected. This ambient temperature is also the temperature to be used in calculating the heat transfer by free convection, Θ_{a2}.

The generator frame will radiate to or acquire heat from surrounding objects, depending on the relative temperature of the bodies involved. This transfer will remain substantially independent of altitude.

Air Flow. Previous equations have shown that the weight flow of air W through a self-ventilated generator is directly proportional to the air density and therefore decreases with altitude.

An inspection of equations 8, 10, and

11 now shows that the watts loss which may be dissipated by a self-ventilated generator, for a constant maximum-allowable surface temperature, decreases with increase in altitude. All quantities in these equations are constant except the air flow W, the air pressure p, and the air density ρ, all of which decrease with altitude. The reduction in rating depends on the relative proportions of q_{for}, q_{free}, and q_R acting to cool the generator. Table I shows the per cent watts loss dissipated by each of the three heat-transfer processes at an altitude of 36,000 feet as compared with the dissipation at sea level (taken as 100 per cent). For example, if the generator could dissipate all of its loss by radiation alone, its rating would be independent of altitude, since q_R is independent of air density and pressure. In any actual application all processes will enter, but the watts dissipated by forced convection will be at least 80 per cent of the total for the lightweight high-output generators now used in the modern airplane and therefore will be the limiting feature.

Table I

Comparison of the Watts Loss Dissipated From Self-Ventilated Direct Engine-Driven Aircraft Generators Employing Different Proportions of Heat-Transfer Processes in Their Cooling

Per Cent of Total Watts Dissipated at Sea Level*			Per Cent of Sea-Level Watts Dissipated at 36,000 Feet*			
By Forced Convection $\gamma=0.67, \alpha=0.5$	By Free Convection	By Radiation	By Forced Convection $\gamma=0.67, \alpha=0.5$	By Free Convection	By Radiation	Total
100	0	0	27	0	0	27
0	100	0	47	0	0	47
0	0	100	0	0	100	100

* Maximum generator-winding surface (140 degrees centigrade), generator-frame surface, surrounding object, and engine-accessory-compartment (38 degrees centigrade) temperatures are constant for all comparisons.

Figure 3 shows the reduction in the watts loss that may be dissipated by forced convection in a self-ventilated generator, as a function of altitude, for constant maximum surface temperature and constant generator speed. Curves are plotted for various values of α in equation 8, and for various ratios of air rise to machine temperature rise at sea level γ. In self-ventilated machines this latter ratio is usually greater than 0.5.

After the total watts loss which can be dissipated by the three processes of heat transfer at a given altitude has been calculated, the new current rating of the generator at that altitude is found from the curve giving the generator line current as a function of its watts loss. This curve is approximated by a constant, representing the no-load losses of the machine plus a term which is a function of the square of the line current.

If the watts which can be dissipated at the operating altitude of the airplane are less than the no-load losses, the machine cannot even maintain its voltage at no load. Figure 3 shows that the self-ventilated generator thus has a "ceiling" of operation at which point the machine can dissipate its no-load losses only and can carry no load current without exceeding its maximum allowable temperature. Since the no-load losses of a lightweight high-output generator may be one quarter to one third of its total losses when carrying rated current, this ceiling may occur within the operating limit of the airplane. At 35,000 feet, for example, the self-ventilated generator can dissipate only 25 to 35 per cent of the losses it can at sea level.

Enclosed Separately Ventilated Generator

Ambient Temperatures. The ambient temperature of the cooling air Θ_{a1} of a separately ventilated generator is that of the air outside of the airplane (neglecting a small temperature rise of the air at the entrance to the air scoop due to its compression). The Air Corps standard atmospheric air temperature decreases with altitude at a rate of -2 degrees centigrade for every 1,000-foot increase in altitude (Figure 1). The ambient temperatures for free convection and radiation remain the same as for the self-ventilated generator.

Air Flow. The minimum ramming-head cooling-air pressure available at the air scoop of an airplane is independent of altitude. From equation 16, therefore, the minimum weight flow of air in a separately ventilated generator is proportional to the square root of the air density.

Equations 10 and 11 show that the same watts loss is dissipated by free convection and radiation in the case of the separately ventilated generator as for the self-ventilated machine.

Figure 4 shows the variation in the watts loss that may be dissipated by a separately ventilated generator for constant ramming-head cooling-air pressure, constant maximum surface temperature, and constant generator speed. Curves are plotted for various values of α in equation 9 and for various ratios of air rise to machine temperature rise at sea level γ.

After determining the total watts loss which can be dissipated by the three processes of heat transfer, the altitude rating of the generator can be found from the curve of watts loss as a function of line current, as was done for the self-ventilated generator.

From Figure 4 and equations 10 and 11 it is apparent that a separately ventilated generator, which obtains its cooling air from an air scoop on the airplane, maintains and may even increase its sea-level rating at practically all altitudes within the operating range of present-day aircraft. In the case of the separately ventilated generator, the reduction in cooling-air ambient temperature offsets the decrease in weight flow of cooling air. Also, due to the constancy of the ramming-head cooling-air pressure of the separately ventilated generator, the weight flow of cooling air decreases only with the square root of the air density. In the self-ventilated gen-

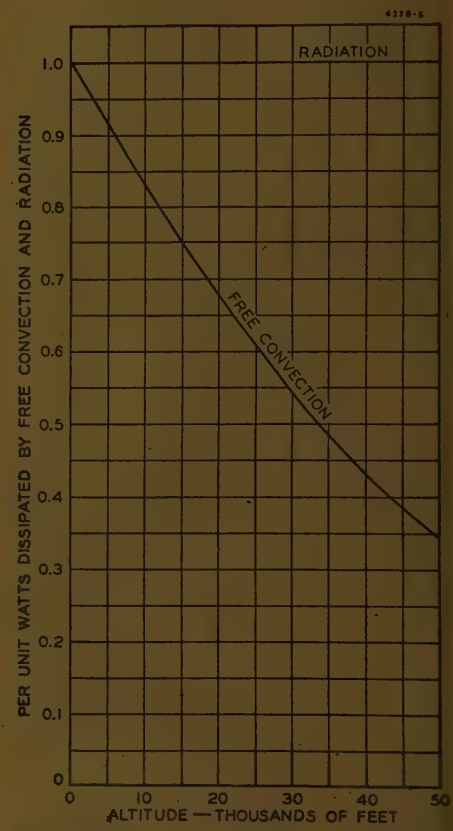


Figure 5. Effect of altitude on the watts loss dissipated by free convection and radiation from self- and separately ventilated direct engine-driven aircraft generators

Constant generator frame surface, engine-accessory compartment, and surrounding object temperatures

erator the weight flow decreases directly with the air density.

Conclusions

A. Figure 3 shows that the rating of an enclosed self-ventilated direct engine-driven d-c aircraft generator decreases rapidly with altitude, and may have a "ceiling" within the altitude range of the airplane on which it is mounted. This ceiling is the point at which the generator will just dissipate its no-load losses without overheating.

B. Figure 4 shows that the rating of an enclosed separately ventilated direct engine-driven d-c aircraft generator, obtaining its cooling air from an air scoop mounted on the airplane, remains essentially independent of altitude—the rating increasing slightly over its sea-level value up to 20,000 feet and then decreasing again.

C. Sea-level tests can be taken, by means of which the rating of a given generator at a given altitude can be determined if ambient conditions at that altitude are known (see appendixes).

D. The analysis presented in this paper can be used to determine the air-flow requirements necessary to obtain a given rating from a given generator at any desired altitude (see appendixes).

E. The results of this analysis can be applied to the determination of the altitude heating and rating of types of electric apparatus other than aircraft generators, if the assumptions made here are critically reviewed and adapted to meet the new conditions.

F. While the effect of brush and commutator heating on limiting the altitude rating of a generator has not been discussed, this problem can be analyzed using the formulas applying to a self-ventilated machine, since whether the machine is self- or separately ventilated the peripheral velocity of the commutator is large compared with the axial velocity of the cooling air (see *Forced Convection* under *Fundamental Analysis*).

G. This paper presents some of the fundamentals of the problem of altitude rating of electric machines and has proposed tests for its determination. It is hoped that future test data, obtained both in altitude chambers and under actual operating conditions, will be correlated and used to modify and expand the analysis where necessary.

Appendix A. Sea-Level Tests and Altitude Performance Calculations for Generators Designed for Aircraft Applications

The tests proposed here are based on the assumption that the point at which the maximum-allowable surface temperature Θ_{s1}' is measured at sea level remains the hottest point in the machine as the load on the machine is varied. Tests on aircraft-type generators have indicated that this assumption is correct. In the proposed tests the generator mounting should be insulated thermally from the test stand on which it is mounted, in order to eliminate the effects of conduction. It is also assumed that the outer surface of the generator remains essentially at constant temperature for a constant maximum internal surface temperature. This assumption has been checked by altitude-chamber tests.

A. Preliminary Tests

These preliminary sea-level tests determine the weight flow of cooling air forced through the generator for various ramming-head pressures at the entrance to the generator cooling system, the watts loss of the machine for various line currents, and the value of the exponent α in equations 8 and 9.

1. Measure the volume flow of air through the generator for various inlet ramming-head air pressures. Set up the generator as a separately ventilated machine and operate at minimum rated speed. Vary the volume flow from a value at which the ratio of the axial velocity of the cooling air to the peripheral velocity of the rotor is approximately 2, to a value at which this ratio is approximately 4.

2. Plot a curve of weight flow of air as a function of the ramming-head pressure of cooling air from the results obtained in section *A*-1.

3. Calculate or determine from dynamometer tests the watts loss of the generator as a function of its line current with the generator operating at its minimum rated speed and rated maximum-allowable surface temperature. Plot a curve of total watts loss as a function of line current.

4. Take a series of heat runs on the generator at minimum rated speed at various inlet ramming-head air pressures, covering the range of pressures used in section *A*-2. At each pressure vary the generator load current until the temperature of the hottest surface in the machine is equal to the maximum-allowable surface temperature. Keep the generator ambient temperature Θ_{a2} at the same value as is expected during actual operation.

5. Calculate the watts dissipated by free convection and radiation in each of the runs of section *A*-4 from equations 10 and 11. Subtract these values from the total watts dissipated in each of the runs, and plot a curve of watts dissipated by forced convection as a function of inlet ramming-head air pressure.

6. Rewrite equation 9 as:

$$\log k_2 + (1-\alpha)\log W = \log\left[\frac{W(\Theta_{s1}'-\Theta_{a1})}{q_{for}} - 0.133\right]$$

Plot W as a function of

$$\left[\frac{W(\Theta_{s1}'-\Theta_{a1})}{q_{for}} - 0.133\right]$$

on log–log paper for various values of inlet ramming-head air pressure. Select a value of this pressure and find q_{for} from the curve of section *A*-5 and W from the curve of section *A*-2 for this pressure. Using these values of W and q_{for}, plot one point of the aforementioned curve. Repeat, using a different pressure.

7. Obtain the slope of the curve of section *A*-6 and calculate α, knowing that this slope is equal to $(1-\alpha)$.

B. Self-Ventilated Generator

To find the altitude rating of a self-ventilated aircraft generator, proceed as follows:

1. Take a heat run at minimum rated speed at such a line current that the temperature of the hottest surface in the machine is equal to the maximum-allowable surface temperature for the generator insulation. Take this heat run in an ambient temperature equal to the expected engine-accessory-compartment temperature at the altitude at which the generator is to be rated.

2. Measure the volume flow of air Q through the machine under the ambient conditions of section *B*-1, and calculate the weight flow of cooling air through the machine.

3. From the watts loss-current curve of section *A*-3, determine the total watts loss of the machine at the load of section *B*-1.

4. Calculate the watts dissipated by free convection and radiation in the heat run of section *B*-1 from equations 10 and 11.

5. Determine the watts dissipated by forced convection in the heat run of section *B*-1 by subtracting the watts dissipated by free convection and radiation from the total watts dissipated.

6. Calculate k_1 in equation 8, using temperatures and the weight flow of air found in sections *B*-1 and *B*-2, the value of α found in section *A*-7, and the value of q_{for} found in section *B*-5.

7. Calculate the total watts dissipated at the altitude at which the generator rating is to be determined from equations 8, 10, and 11. Use values of air density and pressure, and engine-accessory-compartment temperature at the given altitude. The weight flow of air in a self-ventilated generator is directly proportional to the air density. Use values of Θ_{s1}', Θ_{s2}, Θ_{s3}, Θ_{a1}, Θ_{a2}, and Θ_{a3} from the heat run of section *B*-1.

8. Determine the altitude current rating of the generator by entering the watts loss-current curve of section *A*-3 with the total watts dissipated as calculated in section *B*-7

C. Separately Ventilated Generator

To find the altitude rating of a separately ventilated generator, proceed as follows:

1. Determine the minimum ramming-head cooling-air pressure in inches of water available at the entrance to the generator cooling cap at the altitude at which the rating of the generator is to be found.

2. Determine the watts loss that can be dissipated by forced convection by the generator with this ramming-head pressure at sea level from the curve of section *A*-5.

3. Calculate the value of k_2 from equation 9, using the watts loss found in section *C*-2, the weight flow of air from section *A*-2 (correcting for any difference between the cooling-air temperatures of the runs in sections *A*-2 and *C*-2 in determining the weight flow), the temperatures from the heat runs of section *A*-4, and the value of α from section *A*-7.

4. Calculate the total watts dissipated at the altitude at which the generator rating is to be determined from equations 9, 10, and 11. Use values of air pressure and cooling-air temperature and density from Figure 1. The weight flow of cooling air in a separately ventilated generator is proportional to the square root of the cooling-air density. Use values of Θ_{s1}', Θ_{s2}, Θ_{s3}, Θ_{a2}, and Θ_{a3} from the heat run of section *A*-4, corresponding to the minimum ramming-head cooling-air pressure of section *C*-1.

5. Determine the altitude current rating of the generator by entering the watts loss-current curve of section *A*-3 with the total watts dissipated as calculated in section *C*-4.

Appendix B. Determination of the Altitude Rating or the Required Ramming-Head Pressure of a Separately Ventilated Aircraft Generator

By the use of certain simplifying assumptions the current which a separately ventilated generator will deliver at altitude, or, conversely, the ramming-head pressure required to obtain a given current at altitude, can be determined from a single sea-level heat run on the generator.

In the following analysis the value of α in equation 9 is assumed to be unity; the effects of convection, radiation, and conduction in cooling the machine are neglected; and the watts loss of the machine is assumed to vary as the square of the generator line current. Assuming α to be unity gives a

higher value of the watts which may be dissipated by forced convection than is actually the case, but the omission of the watts dissipated by other processes offsets the effect of this first assumption. Finally, in a separately ventilated generator, the I^2R losses are usually at least three to four times the fixed losses, so that at the generator's normal rating the total losses vary approximately as the square of the current.

From equations 7 and 16, therefore:

$$q_{for} = K_3 I^2 = \frac{W}{0.133 + K_2}(\Theta_{s1}' - \Theta_{a1})$$

$$= \frac{\sqrt{\frac{58\rho H}{R}}}{0.133 + K_2}(\Theta_{s1}' - \Theta_{a1})$$

$$I = K_4 \sqrt[4]{\rho H (\Theta_{s1}' - \Theta_{a1})^2}$$

If we let the subscripts A denote the value of a quantity at any altitude, and 0 denote the value at sea level, then:

$$I_A = I_0 \sqrt[4]{\left(\frac{\rho_A}{\rho_0}\right)\left(\frac{H_A}{H_0}\right)\left[\frac{(\Theta_{s1}' - \Theta_{a1})_A}{(\Theta_{s1}' - \Theta_{a1})_0}\right]^2} \text{ amperes} \quad (17)$$

or:

$$H_A = H_0 \left(\frac{\rho_0}{\rho_A}\right)\left(\frac{I_A}{I_0}\right)^4 \left[\frac{(\Theta_{s1}' - \Theta_{a1})_0}{(\Theta_{s1}' - \Theta_{a1})_A}\right]^2 \text{ inches of water} \quad (18)$$

From a single sea-level heat run, then, I_0, ρ_0, H_0, and $(\Theta_{s1}' - \Theta_{a1})_0$ are determined. Knowing these, and the density, inlet air temperature, and allowable hot-spot temperature at the altitude at which the generator is to operate, the current which the machine will deliver without exceeding its temperature rise for a given ramming-head cooling-air pressure can be calculated from equation 17. Also, the ramming-head pressure required for a given current rating can be calculated from equation 18.

Altitude-chamber tests have been taken, and altitude ratings and pressure heads calculated from equations 17 and 18 have checked almost exactly with test values.

For a more fundamental calculation of altitude performance, the methods of Appendix A should be used.

Nomenclature

A_1 = area of machine surface for forced convection, square inches

A_2 = area of machine surface for free convection, square inches

A_3 = area of machine surface for radiation, square inches

A_d = total area of machine ducts through which cooling air flows, square inches

β_m, β_n = numerical constants depending on type of change of air-flow section

C_{self} = over-all thermal conductance between machine surface and cooling air for self-ventilated machines, watts per degree centigrade

C_{sep} = over-all thermal conductance between machine surface and cooling air for separately ventilated machines, watts per degree centigrade

ρ = density of cooling air at entrance to machine, pounds per cubic foot

ϵ = emissivity of radiating surface

$\gamma = \frac{\text{air temperature rise}}{\text{machine temperature rise}}$

g = gravitational constant, feet per minute squared

H = pressure head available for forcing cooling air through machine, inches of water

$L = \frac{L_h L_v}{L_h + L_v}$, feet

L_h = horizontal length of machine for free convection, feet

L_v = vertical height of machine for free convection, feet

p = atmospheric pressure, inches of mercury

q_{for} = watts loss dissipated by forced convection

q_{free} = watts loss dissipated by free convection

q_R = watts loss dissipated by radiation

Q = volume flow of cooling air through machine measured at entrance, cubic feet per minute

R = resistance of machine to flow of cooling air

Θ_{a1} = ambient air temperature for forced convection, degrees centigrade

Θ_{a2} = ambient air temperature for free convection, degrees centigrade

Θ_{a3} = ambient air temperature for radiation, degrees centigrade absolute

Θ_{ar} = cooling-air temperature rise (air rise) degrees centigrade

Θ_d = temperature difference between machine surface and cooling air (surface drop), degrees centigrade

Θ_{s1} = machine surface temperature for forced convection, degrees centigrade

Θ_{s2} = machine surface temperature for free convection, degrees centigrade

Θ_{s3} = machine surface temperature for radiation, degrees centigrade absolute

v = velocity of cooling air past machine surface being cooled, feet per minute

v_a = axial velocity of cooling air through ducts of machine, feet per minute

v_p = peripheral velocity of rotor, feet per minute

W = weight flow of cooling air through machine, pounds per minute

Primed quantities are values measured at the cooling-air exit of the machine.

Reference

1. The Basic Laws and Data of Heat Transmission, W. J. King. *Mechanical Engineering*, March 1932, pages 190–4; April 1932, pages 275–9 and page 296; May 1932, pages 347–53; June 1932, pages 410–14, and page 426; July 1932, pages 492–7; August 1932, pages 560–5.

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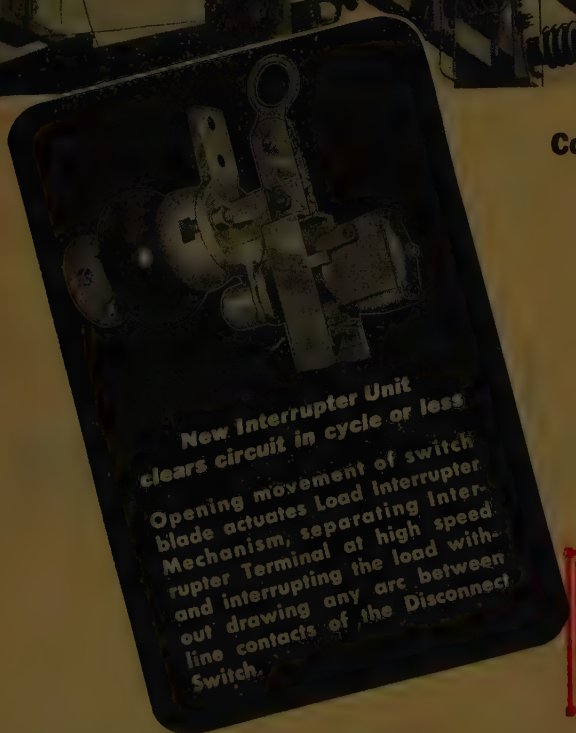


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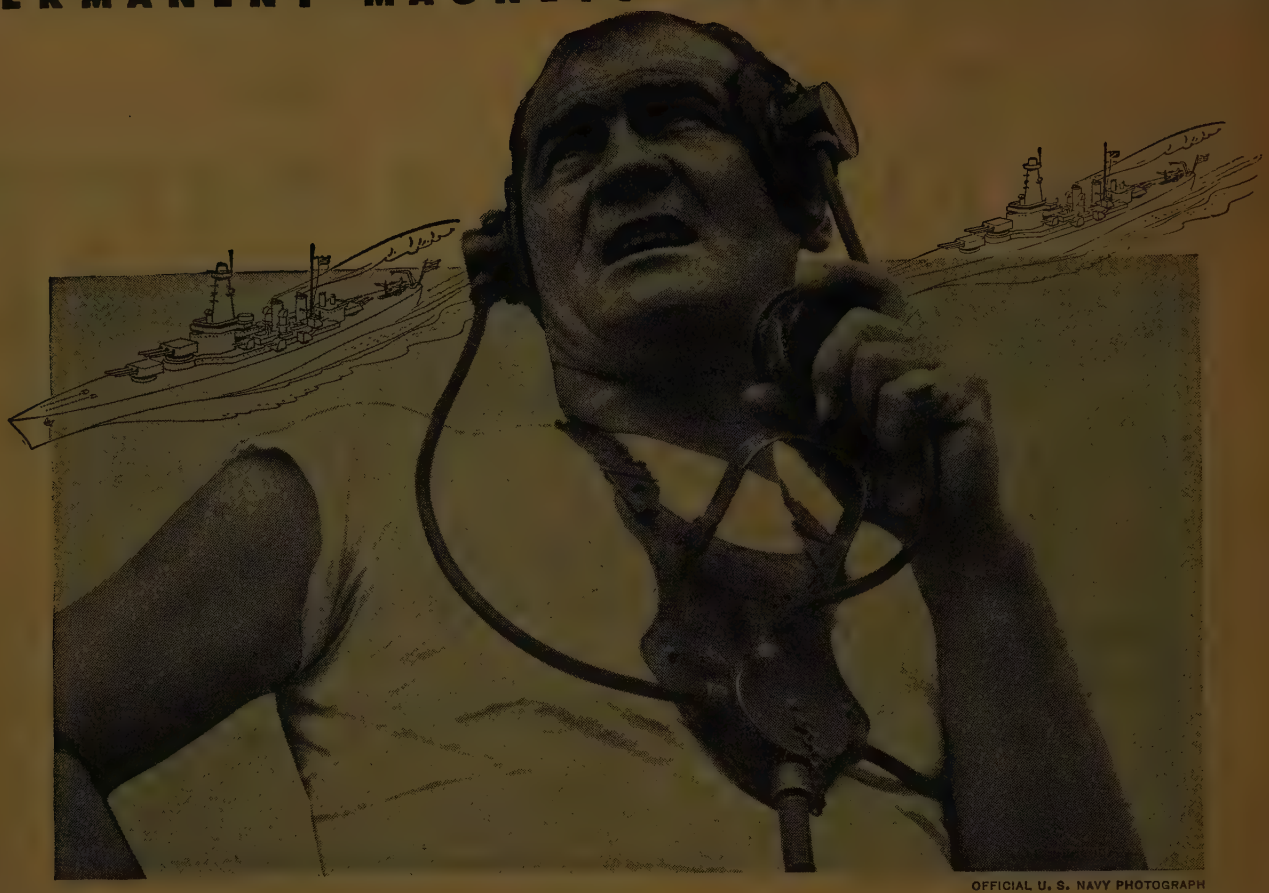
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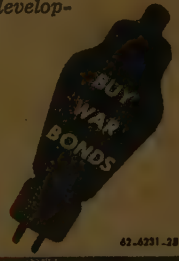
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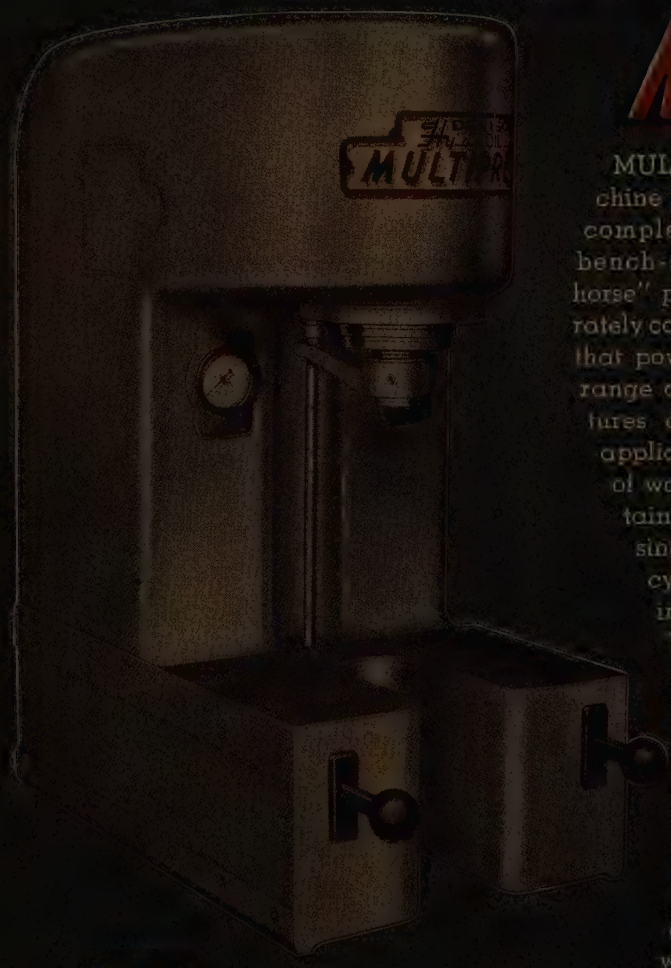
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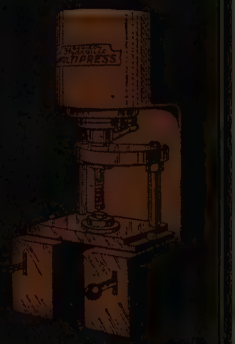
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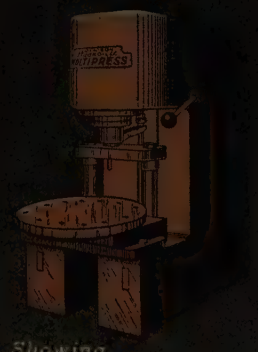
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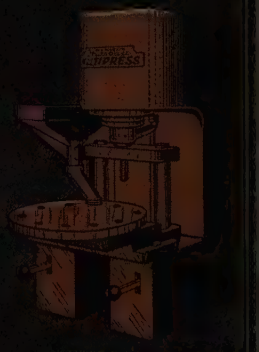
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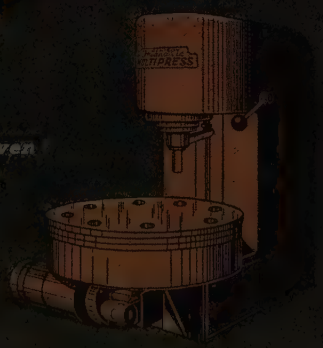
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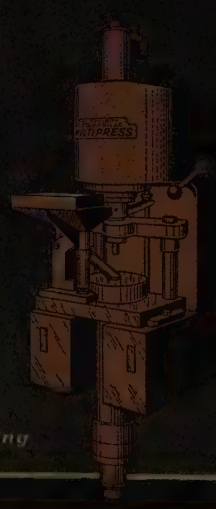
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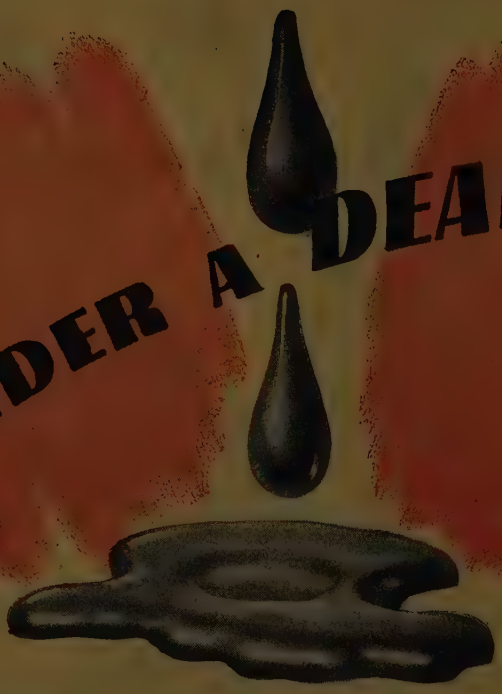
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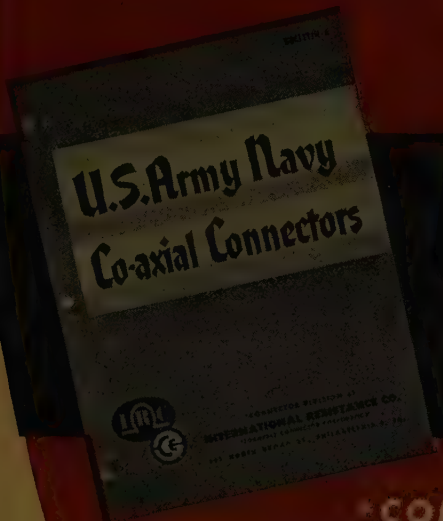
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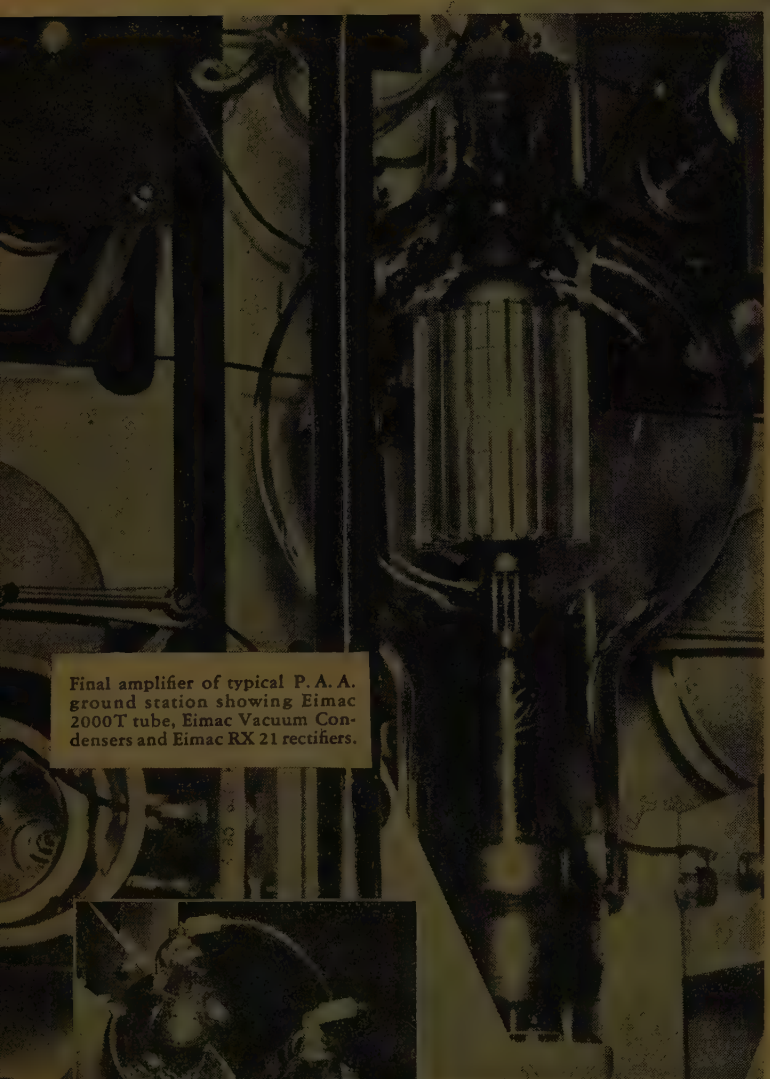
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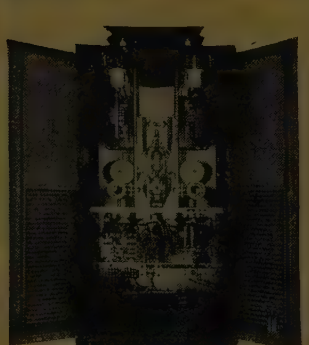
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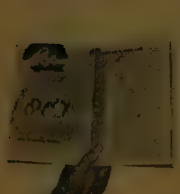
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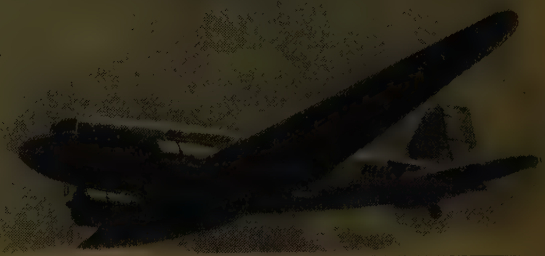


Final amplifier of typical P. A. A. ground station showing Eimac 2000T tube, Eimac Vacuum Condensers and Eimac RX 21 rectifiers.





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Because of the extensive operations of Pan American World Airways, these tubes have been subjected to about every test possible — altitudes; ground level; extremely cold climates and high temperatures found at the equator; conditions of high and low humidity; and in some instances, when new bases are being built, perhaps somewhat trying power conditions. The high regard which P. A. A. engineers have for Eimac tubes is clearly evidenced by their continued and more extensive use, as the years roll by.

The fact that Eimac tubes are the number one favorite of the commercial airlines is important evidence to substantiate the oft repeated statement that "Eimac tubes are first choice of leading electronic engineers throughout the world."

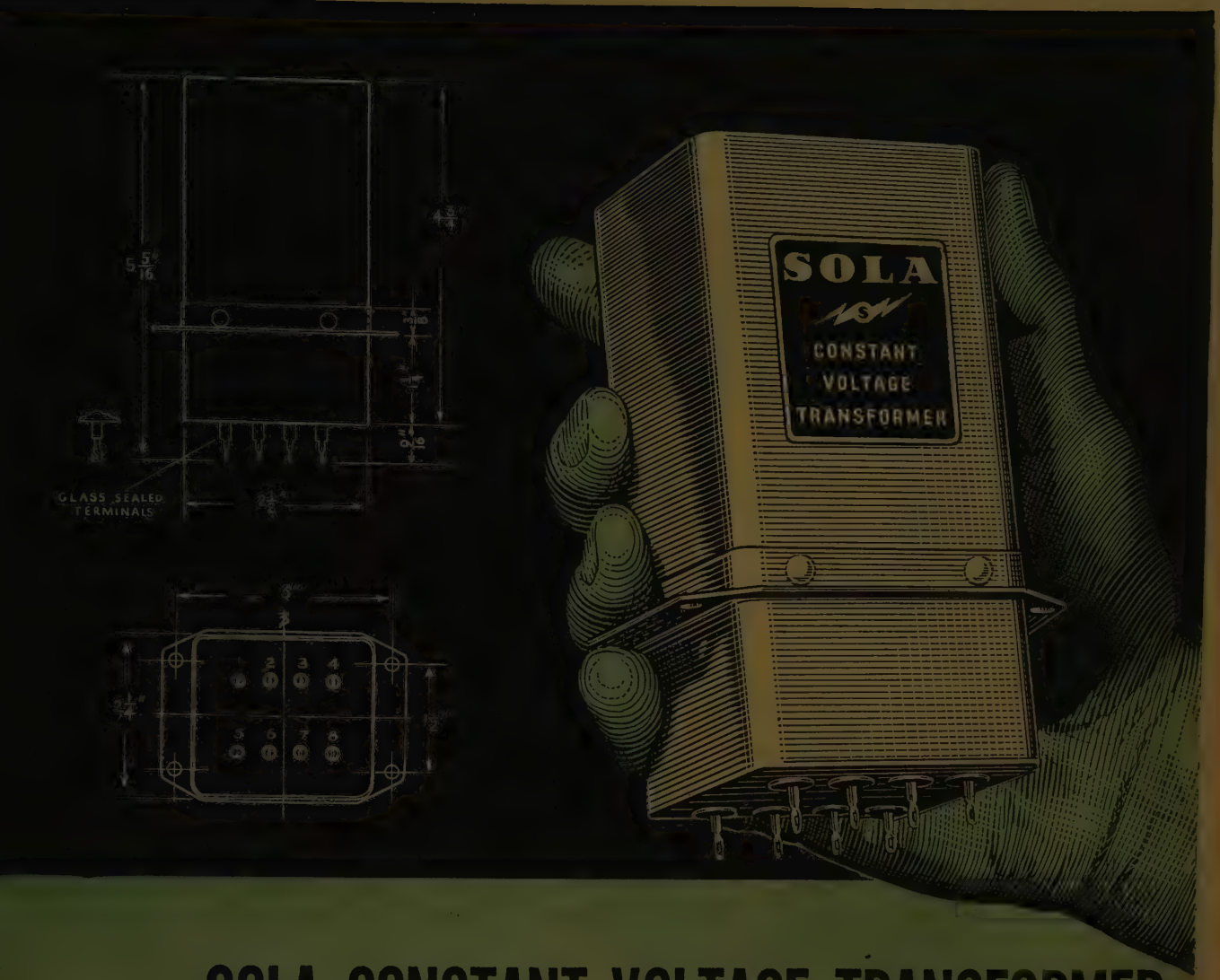
Follow the leaders to

Eimac REG. U. S. PAT. OFF. TUBES

EITEL-McCULLOUGH, INC., 947 San Mateo Ave., SAN BRUNO, CALIF.

PLANTS LOCATED AT: SAN BRUNO, CALIFORNIA AND SALT LAKE CITY, UTAH

Export Agents: FRAZAR & HANSEN, 301 Clay Street, San Francisco, California, U. S. A.



Another SOLA CONSTANT VOLTAGE TRANSFORMER

that has an important future in *YOUR* postwar plans

There may be one vulnerable spot in the design of *your* equipment that this SOLA Constant Voltage Transformer will correct.

Your customers do not have the stable line voltage called for on your label. They will blame *your* equipment for inefficient operation, *not* the fluctuating voltages that really cause it.

Build this SOLA Constant Voltage Transformer into your product and you can be certain that the operating voltages will always be within ±1% of rated requirements regardless of line fluctuations as great as 30%.

This SOLA Constant Voltage Transformer is built to fit your equipment. (Note the small, compact dimensions.) Its low cost will fit your production budget. Its automatic operation will eliminate the need for other costly components, and relieve your customers of the responsibility of making manual voltage adjustments.

There's a powerful sales story behind a product equipped with a SOLA Constant Voltage Transformer.

Constant Voltage Transformers

To Manufacturers:
Built-in voltage control guarantees the voltage called for on your label. Consult our engineers on details of design specifications.
Ask for Bulletin ACV-100

Transformers for: Constant Voltage • Cold Cathode Lighting • Mercury Lamps • Series Lighting • Fluorescent Lighting • X-Ray Equipment • Luminous Tube Signs • Oil Burner Ignition • Radio • Power • Controls • Signal Systems • Door Bells and Chimes • etc. **SOLA ELECTRIC CO., 2525 Clybourn Ave., Chicago 14, Ill.**





Engineering Resourcefulness

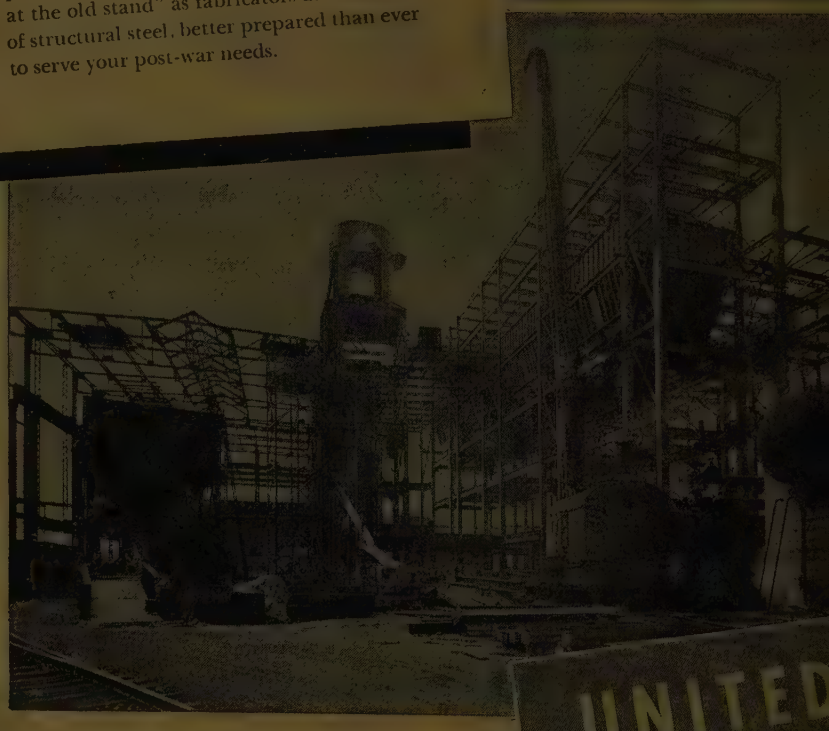
—a "plus" for your structural jobs

DURING the many years we have fabricated and erected steel structures for the electric power industry, one characteristic of American Bridge service has had more to do with the success of the jobs we have handled than anything else. It isn't a tangible factor. We can't write it into contracts. But every project benefits from it. You may have heard it called *engineering resourcefulness.*

What is this quality worth to you? . . . It means that every job American Bridge undertakes—whether it is the steelwork for a generating station, the towers for a transmission line, a substation structure, or an electrification project—will profit from an unsurpassed background of structural experience. It means that each project, no matter how large or how small, will receive the same painstaking care in planning, fabrication and construction.

Engineering resourcefulness gave our organization its worldwide scope of operation. This same resourcefulness now is enabling us to serve the war needs of our country in numerous ways, including even the manufacture of high-precision military equipment. We value this opportunity. But when peace returns, you'll find us "doing business at the old stand" as fabricators and erectors of structural steel, better prepared than ever to serve your post-war needs.





AMERICAN BRIDGE COMPANY

General Offices, Frick Building, Pittsburgh, Pa.

District Offices in: **Baltimore · Boston · Chicago · Cincinnati · Cleveland Denver · Detroit · Duluth · Minneapolis · New York · Philadelphia · St. Louis**

Columbia Steel Company, San Francisco, *Pacific Coast Distributors*
United States Steel Export Company, New York

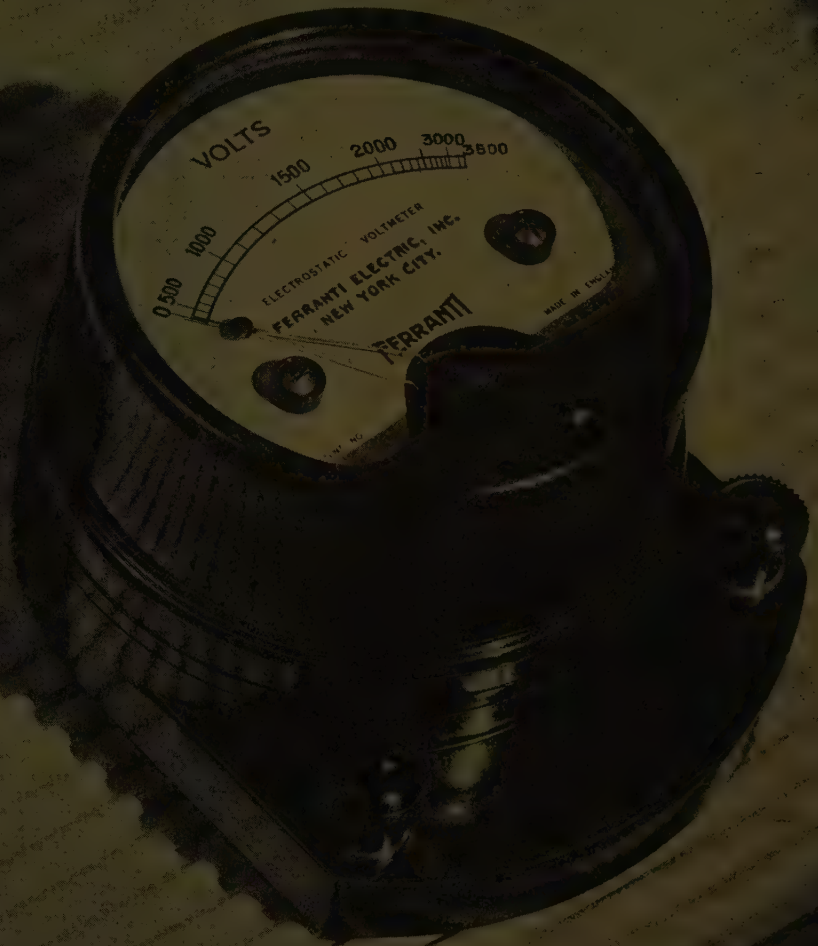
USS

UNITED STATES STEEL

ELECTROSTATIC VOLTMETER

PORTABLE
PROJECTING
FLUSH

For Accurate Measurement In High Impedance Circuits



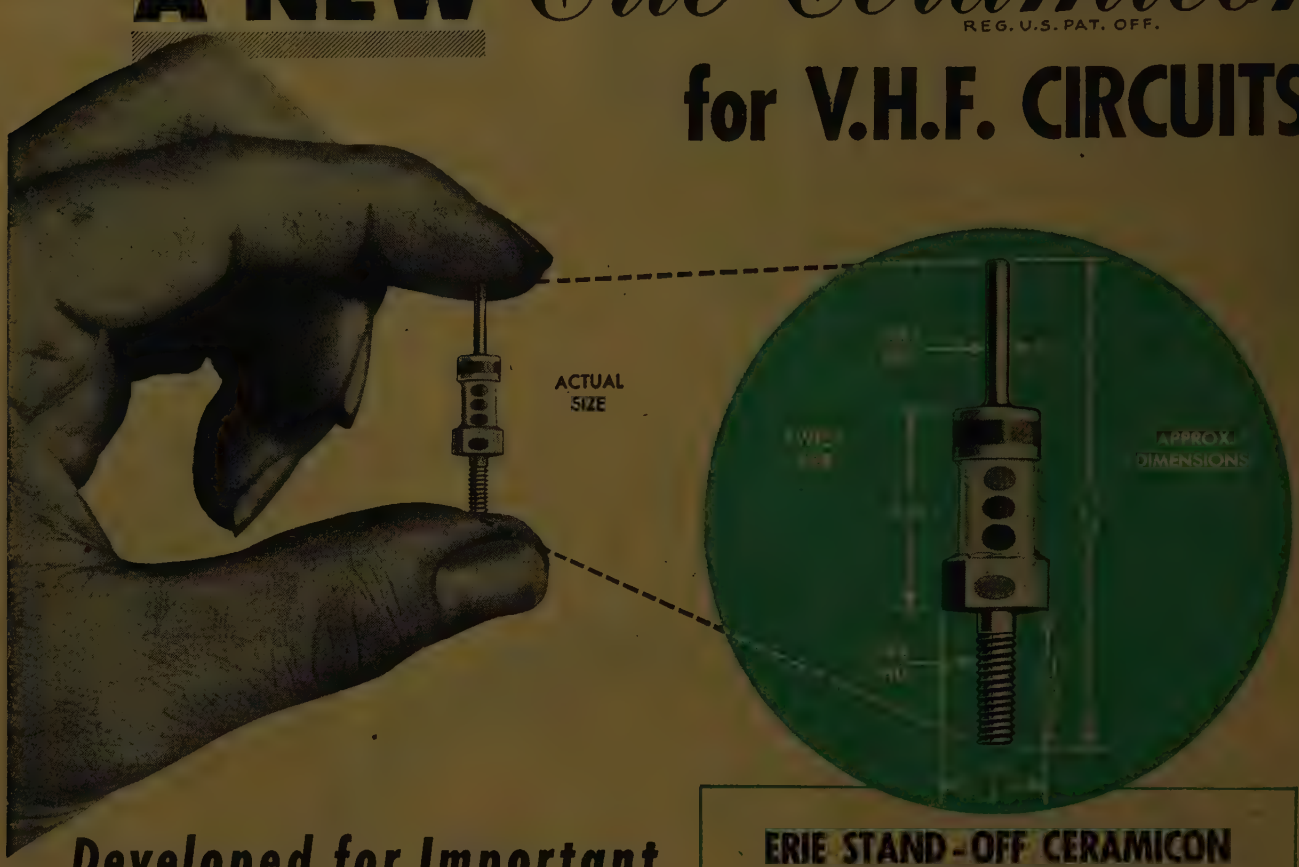
RANGES
0- 150 Volts
0- 300 Volts
0- 450 Volts
0- 600 Volts
0- 750 Volts
0-1000 Volts
0-1500 Volts
0-2000 Volts
0-2500 Volts
0-3000 Volts
0-3500 Volts

FERRANTI ELECTRIC, INC. 30 ROCKEFELLER PLAZA NEW YORK, N. Y.

Ferranti Electric, Ltd., Toronto, Canada · Ferranti, Ltd., Hollinwood, England

A NEW *Erie Ceramicon* REG. U.S. PAT. OFF.

for V.H.F. CIRCUITS



Developed for Important War Application—Ideal for Peacetime F.M. and Television

HIGH Frequency applications have a way of throwing existing component designs into the discard. Short, heavy leads, low series inductance, and other design factors assume greater importance.

Erie Resistor had already had considerable experience in designing tubular and disc ceramic capacitors, and button type silver mica condensers for V.H.F. and U.H.F. equipment. Thus, when engineers from one of the country's foremost research and development laboratories came to Erie Resistor with a new capacitor design problem, the answer was quickly forthcoming, in the form of a basically new style of Erie Ceramicon, pictured above. Approximately 60 days later, finished units were being delivered for the extremely urgent communications equipment for which they were designed.

ERIE STAND-OFF CERAMICON

Maximum Capacity
17 MMF.—zero temperature coefficient
30 MMF.—330 P/M/°C " "
75 MMF.—750 P/M/°C " "
Minimum Capacity, 2 MMF.

This compact, stand-off Ceramicon has mechanical advantages that permit it to be rigidly mounted, and support other circuit elements. Electrically, this Erie stand-off Ceramicon has a high resonant frequency, and short electrical path to both silvered plates. The stability and retrace characteristics of Erie temperature compensating Ceramicons are inherent in this new unit.

Is there a place for this new component in your designs for war or peacetime communications equipment? Or if you have any other design problems involving resistors or condensers, our engineers will be glad to discuss them with you.

ARMY E NAVY
FOR HIGH ACHIEVEMENT IN WAR PRODUCTION

ERIE RESISTOR CORP., ERIE, PA.

LONDON, ENGLAND · TORONTO, CANADA.

★ ★ ★ Let's All Back The Attack—Buy EXTRA War Bonds. ★ ★ ★

NEW FREQUENCY METER and MONITOR

for HIGH FREQUENCY SERVICES—1500 kc to 200 Mc



For measuring and monitoring the carrier frequency of a-m transmitters, these two new G-R instruments offer many operating advantages over equipment formerly available.

With the Frequency Meter, readings are substantially independent of amplitude of modulation, input waveform and input voltage. Over very wide ranges, changes in any of these do not affect the meter indications. The instrument requires no direct connection to the transmitter . . . a foot or two of wire provides ample coupling. The indicating meter has six ranges with full-scale values of 200 cycles, 600 cycles, 2 kc, 6 kc, 20 kc and 60 kc.

One of the most useful features of the Frequency Monitor is its great sensitivity. It can be used to monitor mobile stations. The numerous operating conveniences include: a panel switch to select any one of four temperature-controlled quartz plates; a "stand-by" control to maintain operating temperature continuously with the tube circuits disconnected; positive indication of the direction of frequency deviation; panel terminals for the audio output and for the output of the crystal buffer stage for calibrating or adjusting transmitters or receivers.

You'll find that this combination of instruments is one of the best G-R has developed for high-frequency communications monitoring.

Because we are in full-time production of war orders, none of these instruments are available for shipment, and probably will not be until after the war. We ARE accepting reservation orders, however, and will fill them in rotation as soon as production starts.

FREQUENCY METER

RANGE: 0 to 60,000 cycles in six ranges

ACCURACY: ±2% of full scale

INPUT VOLTAGE: Any between 0.25 and 150 volts

MOUNTING: Relay-rack panel; walnut end-frames (illustrated) for table mounting, extra

TYPE 1176-A FREQUENCY METER $185.00

FREQUENCY MONITOR

CARRIER RANGE: 1500 kc to 200 Mc

ACCURACY: 0.003% with our quartz plates

QUARTZ PLATES: Up to four, not included in price; ground to channel frequency

MOUNTING: Same as Frequency Meter

TYPE 1175-A FREQUENCY MONITOR $250.00

GENERAL RADIO COMPANY

CAMBRIDGE 39, MASSACHUSETTS
New York 6
Chicago 5
Los Angeles 38



FINEST PICTURE QUALITY IN BLACK AND WHITE AND IN FULL COLOR!

Sharper, more brilliant pictures than ever before possible are now a reality with Federal's new broadband television technique . . .

In a revolutionary contribution to the television art, Federal's system permits combining *sight and sound* on one carrier frequency . . .

For the broadcaster—a single transmitter, and consequently, lower first cost, lower power consumption, less space requirement, and fewer high power tubes . . .

For the television audience — a simpler, less expensive receiver, more compact and efficient, and requiring fewer tubes.

This great forward stride is the logical outcome of Federal's long list of achievements in the field and the contribution of Federal's engineers to the development of the "Micro-ray" more than a decade ago . . . the forerunner of modern television technique.

And as a result . . . Federal has been selected by the Columbia Broadcasting System for the construction of its new television transmitte atop the Chrysler Tower in Nev York.

Federal's modern television tech nique will also be reflected in a equally advanced Federal tele vision receiver for the home . . producing the finest picture quality

Federal has the experience, th facilities, the technique, needed t build television equipment for an broadcasting requirement. For th best in television — see Federal firs

FEDERAL TELEPHONE & RADIO — FTR

Federal Telephone and Radio Corporation

AN IT&T ASSOCIATE

Newark 1, N. J.

Another Step Up In Heat Resistance of Electrical Insulation

NATIONAL SILICONE-TREATED MICAGLAS COILS, GLASS TEXTILES AND MICAGLAS

Up to this time the usefulness of the ***heat-resisting*** qualities of Micaglas and other Class B insulating materials has been limited because of the relatively low heat-resistant properties of even the best conventional varnishes, bonds and fillers.

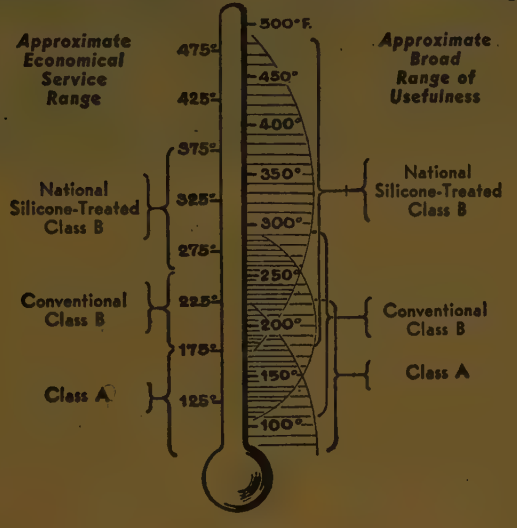
Now Silicone resins have been developed. Extensive tests indicate that some of them will withstand constant temperatures more than 100° F. higher than the previous high point. The net result is that resistance to damage from heat is raised to that extent.

We have combined these new Silicone resins with glass textiles and Micaglas to allow greater use of the heat-resistant qualities of glass and mica.

An idea of the tremendously important result gained is seen in the fact that insulation so treated remains flexible under 482° F. for 50 hours. At continuous 270° F. for ninety days National Silicone-treated coils showed only slight aging. The conventional varnish used in Class B coils of the same kind in the same test was completely disintegrated.

So important is this new technique to operators of rotating electrical machines that it ranks in importance with the introduction of asbestos and fiberglas as a second great step forward in the battle against heat damage.

Insulation Thermometer for Electrical Windings



Much Longer Life for Over-Heated Electric Windings

Obviously, National coils insulated with Silicone-treated Fiberglas or Micaglas offer relief to the operator who is compelled to overload his motors or to operate them in high ambient temperatures. Increased life and reduced coil trouble result. Where either continuous or intermittent heat at or somewhat above the upper limit of conventional Class B insulation causes trouble, National Silicone-treated insulation will vastly improve performance.

Note that Silicone does not affect the heat-resisting qualities of the materials with which it is used. It simply replaces less heat-stable bonding agents and fillers whose low heat-damage levels prevented utilization of the excellent properties of glass, mica, etc., at higher temperatures.

Please Note

Several limiting factors in the use of Silicone varnishes must be taken into consideration. Motor bearings are subject to heat damage. Electrical joints should be brazed rather than soldered. The copper in commutators and slip rings anneals at the curing temperature of Silicone varnishes.

Silicone varnishes should not be used indiscriminately. ***Because of factors other than heat, conventional varnishes may often be used advantageously.***

AVAILABILITY

National Silicone-bonded Fiberglas and Micaglas are already available in restricted quantities. National Silicone-treated sleeving, tubing and similar products are in production, but not yet in sufficient quantities for unlimited use. National Silicone Fiberglass and Micaglas insulated coils can now be produced in limited quantities. If heat is causing persistent trouble give us the facts.

Copyright 1944, National Electric Coil Company, Columbus, Ohio

NATIONAL ELECTRIC COIL COMPANY

COLUMBUS, OHIO

ELECTRICAL ENGINEERS: MAKERS OF ELECTRICAL COILS AND INSULATION—

TRADE MARK

BLUEFIELD, W. VA.

REDESIGNING AND REPAIRING OF ROTATING ELECTRICAL MACHINES







Dips and peaks in line potential need not disturb voltage-sensitive machines and devices. The AmerTran "LC" Transtat Line Voltage Compensators manually correct such fluctuations. They boost or buck voltage as needed. In fact, the "LC" Transtats combine most of the advantages of the famous "TH" Transtats with some that are peculiarly their own.

These advantages include lower cost per KVA of output and close control in increments as low as 0.2 volt without circuit interruption. Air insulated, designed for continuous operation indoors at full load, they are rated from 2 to 85 KVA, for nominal line voltages to 480, adjustment range ± 10% of line voltage. Also available for remote push-button control (LCM). Bulletin 51-2 contains a complete description, ratings, prices and suggested circuits. Write for it.

AMERICAN TRANSFORMER CO.
178 EMMET STREET, NEWARK 5, NEW JERSEY

* Reduces higher than rating line voltages, too. Swelling or sagging voltages are handled with equal ease by "LC" Transtat Line Voltage Compensators.

AMERTRAN

MANUFACTURING SINCE 1901 AT NEWARK

Pioneer Manufacturers of Transformers, Reactors and Rectifiers for Electronics and Power Transmission

AMER TRAN

ELECTROPLATING WITH POWERSTAT CONTROL







In order to conserve vital floor space and to keep the rectifying equipment away from a corrosive atmosphere, an eastern manufacturer specified that the power supply for the plating tanks be located outside the processing room. This meant remote control of tank voltage and current. To satisfy this requirement the engineers of the W. Green Electric Co., builder of rectifier units, replaced the usual tapped transformer with a Motor-driven POWERSTAT Variable Transformer type M1226-3. Installation of a push-button station in the plating room to control the POWERSTAT'S highly damped synchronous reversible motor provided the remote control feature. By simply pressing a button, any current from zero to maximum flows through the plating tanks.

The use of POWERSTATS in electroplating offers other advantages in addition to finger-tip remote control. Regulation is not limited to "steps" but is continuously variable; there is no interruption of current so characteristic of the tap changing system; and POWERSTATS have a long life with no tap switches to wear out.

For specific information on POWERSTAT control of electroplating processes, consult SECO engineers.

Send for Bulletins 149 EE and 163 EE

SUPERIOR ELECTRIC CO., 245 LAUREL STREET BRISTOL, CONNECTICUT

SUPERIOR *Electric Company*



FAST

can build that "Out of the Ordinary" Capacitor

Pictured above are a few Capacitors designed for unusual operation . . . or for some particular application which requires not only a special container and terminals, but a departure from standard internal construction.

Whether it be the tubular wax paper Capacitor . . . the wax filled metal cased type . . . the hermetically-sealed oil impregnated and filled type . . . or the polystyrene types . . . the FAST organization can best meet your requirements.

Instrument Designers, Physicists, Scientists, Researchers, Experimenters—as well as Commercial Organizations planning to build that Electronic Device for tomorrow's market—are invited to avail themselves of our wide experience in the design and production of fine Capacitors. Feel free to consult us the next time you have a particularly vexing Capacitor problem.

"When You Think of Capacitors . . . Think FAST"



CERTIFICATE OF ACHIEVEMENT

It may be of interest to note this is the first time in the history of the United States Navy that any industry has been selected for a citation of honor and achievement. The John E. Fast organization is a member of this group.

JOHN E. FAST & CO.

Capacitor Specialists for a Quarter-Century

3125 North Crawford Avenue, Chicago 41

Canadian Representatives: Beaupre Engineering Works Reg'd.
2101 Bennett Avenue, Montreal, for Power Factor Correction
J. R. Longstaffe, Ltd., 11 King Street, W., Toronto 1, for Special Applications

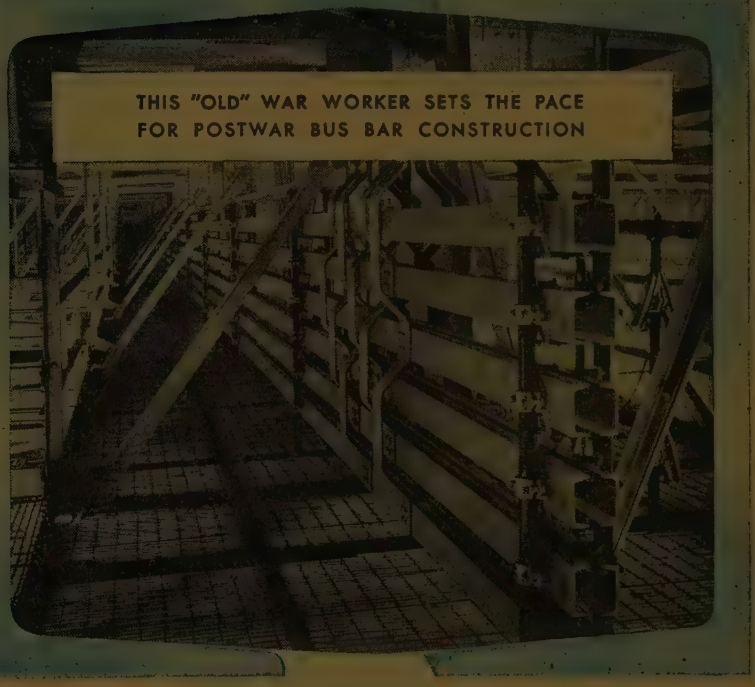
Alcoa Aluminum is Available

FOR BUS BARS AND FITTINGS

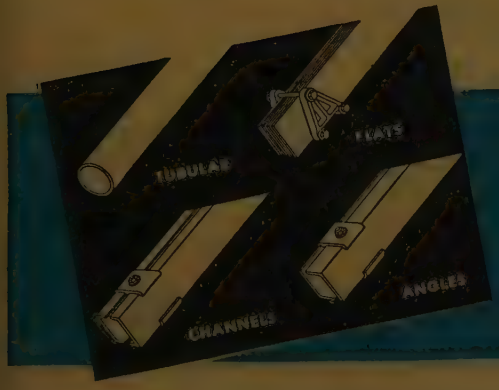
Now that the supply of aluminum exceeds the demands for military uses, Alcoa Aluminum bus bar shapes and fittings are again available. You can proceed with the extension and modernization work held up by previous restrictions.

Less labor is required to install aluminum bus bars. Their lighter weight makes handling and erection easier. Supports can be similarly lighter and more widely spaced. Buses can, therefore, often be installed without expensive reinforcement of structures.

Alcoa engineers will gladly help select conductors and fittings for your bus installations. Write ALUMINUM COMPANY OF AMERICA, 2149 Gulf Building, Pittsburgh 19, Pennsylvania.



The idea of using structural shapes for electrical conductors was originated by Aluminum Company of America; better ventilation and higher efficiency, rigidity and strength of a girder. This first installation of channel bus bars, installed in 1928, has a run of 1,600'. Conductors are 5" Alcoa Aluminum channels.



ALCOA
ALUMINUM

A TELECHRON PRE-SELECTOR FOR POST-WAR RADIOS



THE Telechron Pre-Selector shown here is an important development for post-war radios. It's low in cost — recommended for use with moderate-price receivers.

Buyers will like this easy-to-use Pre-Selector, with finger-tip control. Each of the 48 keys around the Telechron clock dial represents a 15-minute timing interval. Pulling out a key automatically turns the receiver on, at the pre-selected station, for 15 minutes—and off at the end of the period, unless the next key is pulled out also. Keys are automatically reset to OFF position after timing periods are passed.

A flick of the finger sets the keys around the large and legible clock face. There are no knobs to turn, no complicated settings, no difficult calculating. Settings can be made as much as 10 hours in advance.

This Telechron Pre-Selector is only one of a full line of automatic timing and control devices which we can supply for post-war home appliances. All use the famous self-starting, synchronous Telechron motors and movements. For full information about Telechron timers, wire or write Automatic Control Division, Dept. G.



Telechron

REG. U. S. PAT. OFF.

WARREN TELECHRON COMPANY • ASHLAND, MASSACHUSETTS

Present TESTS CANNOT PREDICT *Future* STATION INSULATOR SERVICE—



But You Can Rely Upon O-B's 30-Year Record

Specified M. & E. tests on a switch and bus insulator deal wholly with the present--present strength, present dielectric values. They tell you accurately what you have *now*. But "now" is only one important consideration in station insulation. Ten, twenty, thirty years from now represents service performance and can't be predicted reliably by any known set of tests or specifications.

Has O-B anything concrete to offer in the way of looking into the future? Just this: Thousands of O-B insulators have rounded out thirty years of station service and are still on the job. Big improvements have come about since these pioneers went to work. Today's designs have better reason for such performance.

It's a matter of actual record, gained from the longest station insulator history in the industry, that sound reliability is an integral part of O-B Insulation. You can't assure such future performance by any specifications or tests, but you can get it simply by writing "O-B" on your order.

Ohio Brass

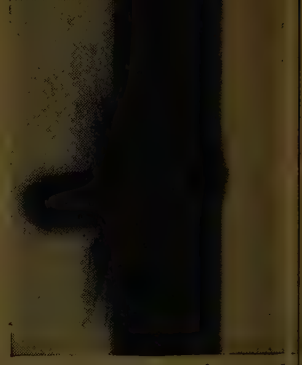
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MANSFIELD, OHIO, U. S. A.

CANADIAN OHIO BRASS COMPANY, LIMITED
NIAGARA FALLS, ONTARIO



PICTURES COURTESY LABORATORY DIVISION, FRANKFORD ARSENAL



High-speed X-ray picture of cal. .30 bullet penetrating 1/2 inch thick armor



High-speed X-ray picture of same bullet 20 millionths of a second later

14,285 times quicker than a wink

Westinghouse research engineers have developed an ultra-high speed X-ray tube that makes possible X-ray pictures, taken at the terrific speed of *one-millionth of a second.* These pictures show armor-piercing bullets penetrating ½ inch of solid steel armor plate.

The action is 10,000 times faster than any conventional X-ray—literally 14,285 times *quicker than a wink!*

Secret of this revolutionary X-ray is the new type tube that can handle a jolt of 2000 amperes, at 300,000 volts. This is applied in a flash by electrostatic condensers—creating a tremendous surge of *X-radiation.*

With this new X-ray, U.S. Army ballistic experts can "freeze" the image of a bullet, while it travels within a gun barrel at 2600 feet per second—or study the action of projectiles as they smash through armor plate.

When peace returns, this new example of Westinghouse *skill in research* will enable machine builders to study the strains in rapidly moving parts—improve performance and increase the life of peacetime products.

Westinghouse Electric & Manufacturing Company, Pittsburgh 30, Pennsylvania.

KEEPING UP WITH *Electricity*

MAN-MADE HURRICANE BLOWS OUT ELECTRICITY. Engineers can now "blow out" electricity as easily as you extinguish the flame from your cigarette lighter. Circuit breakers built by Westinghouse unleash a 600-mile-an-hour blast of compressed air to snuff out powerful short-circuit arcs and prevent damage to vital electrical equipment on power lines. The hurricane of air can smother a 1,000,000-kilowatt electric arc in less than a hundredth of a second.

REPRESENTATIVES OF 257 PRE-WAR PROFESSIONS, businesses, and trades are now employed at the Westinghouse operated Naval Gun Plant at Louisville, Kentucky. Included are: former circus performers, several embalmers, a former professional hill-billy musician, and a pipe-organ builder. Despite their unusual peace-time occupations, all here have been able to learn the amazing high precision needed in making Naval guns.



A NEW GUNSIGHT LAMP that enables American gunners to aim *directly into the sun* and yet fire with deadly accuracy has been developed for the Army and Navy by Westinghouse Lamp Engineers. Former gunsight lamps allowed gunners to aim within only 15 degrees of the sun, leaving a dreaded "blind spot."

FREE . . . "Engineering Highlights of 1944"—a 32 page book, filled with interesting articles on new developments in electrical research and engineering in wartime. Write: Westinghouse Engineer (EE-124).

Westinghouse

PLANTS IN 25 CITIES OFFICES EVERYWHERE

TUNE IN: John Charles Thomas, Sunday 2:30, EWT, NBC; Ted Malone, Mon. Wed. Fri., 10:15 pm, EWT, Blue Network

Vinylite



When deliveries start again...



New car deliveries will prove that happier days are here again. Though they look like prewar models, they'll be better cars—improved in many ways. Harness-type wiring systems wrapped in VINYLITE plastic tape, for instance, will be far safer and more dependable than any former assemblies. This unique tape is used on the multiple circuit harnesses of almost every combat vehicle and truck that's built today.

VINYLITE plastic tape gives wiring harnesses 8-way protection. It is non-flammable, eliminating the fire hazard. It is non-oxidizing and permanently flexible; has high dielectric and high tensile strength; resists chemicals and abrasion. Moisture, oils, and grease find it practically immune to their deteriorating action.

But that's only part of the story. VINYLITE plastic tape revolutionizes the *production* of wiring harnesses. It is applied by standard wrapping procedures, and heat-sealed by baking for only 15 minutes, at 265 deg. F. No varnish is used. No additional baking—no cleaning of terminals. Production time cut from 24 hours to 24 minutes.

Besides their use as insulating tapes, VINYLITE plastics are available as extrusion materials for primary insulation, tubular jacketing, and conduits. Wherever used, they bring new economy and safety to the electrical wire and cable field. You will probably find them a real investment in connection with your own products, present or planned. Write for descriptive booklet 1-VR, "Vinylite Plastics for Wire and Cable Insulation."

BAKELITE CORPORATION

Unit of Union Carbide and Carbon Corporation

UCC

30 EAST 42ND STREET, NEW YORK 17, N.Y.



Plastics



. . . THE "MEGGER"* INSULATION TESTER

The dependability of the "Megger" Instrument for measuring electrical insulation resistance is like the constancy of Ohm's Law, on which principle it actually operates. This "Megger" method for testing insulation resistance is simple and remarkably accurate. The principle is precisely the same as it was forty years ago and yet it meets today's needs perfectly.

You will find the same ruggedness and dependability in U.S.-made "Megger" instruments that the electrical industry has known for so long a time.

Let us send you full details on various types and ratings. Ask for Bulletin 1685-EE.

* *Trade Mark Reg. U. S. Pat. Off.*



PRINCIPLE OF OPERATION

In what we term a cross-coil true ohmmeter, two coils are mounted in fixed relation to each other on the same pivot-and-jewel moving system in the field of a permanent magnet. "Current" flows in coil A and "potential" in coil B, and they are connected so that their respective torques oppose each other. Since there are no control springs, the opposing coils give a true ratio of E/I, and ohms (or megohms) are indicated directly by a pointer over a scale. The readings are independent of the voltage of the hand-driven d-c generator, because any change in the voltage affects both coils in the same proportion.

JAMES G. BIDDLE CO. 1211 ARCH ST. PHILA. 7, PA.



CATHODE-RAY *Oscillography*

★ You're probably losing time and money if you are without a cathode-ray oscillograph. We can prove it in most cases. In the plant, out in the field, in the laboratory, in servicing and maintenance—a DuMont cathode-ray oscillograph is a "must" instrument today.

DuMont offers a wide choice of types to meet all requirements. Here are typical uses: To check wave form and harmonic content. Also to check phase. Useful in checking power factor, welding cycle, relay and circuit breaker operation, etc. Used to check vibration, dynamic balancing of rotating parts, ignition, engine pressures, etc.

★ **Write for Literature**

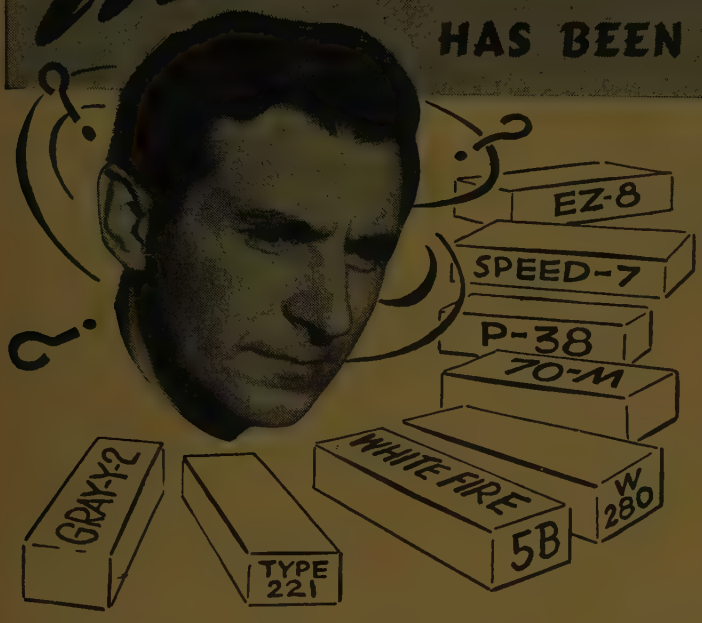
DUMONT

ALLEN B. DU MONT LABORATORIES, INC.
Passaic, New Jersey

Cable Address: Wespexlin, New York

The Art of Buying

ARC WELDING ELECTRODES HAS BEEN GREATLY SIMPLIFIED...



It used to be.. a puzzling situation — those electrodes with confusing trade names and trade numbers. Each manufacturer's identification system was different, each one had his own special name for electrodes. Thus, for example, one particular rod designated by American Welding Society as E-6012 had over twenty different trade names. And no two trade names were even vaguely similar. To a welder selecting a certain rod for a definite job these varying trade names were disconcerting. They had *no* connection with any standard welding classification. Unless he had previously used the rod or took time to check into its classification, he was at a loss to select the correct electrode.

..But now Allis-Chalmers has cut through this mass of confusion and greatly simplified the business of buying electrodes. Official *AWS classification numbers* have been adopted for Allis-Chalmers electrode identification. Thus *AWS E-6012* becomes *Allis-Chalmers E-6012* in the A-C electrode line. It's logical and practical. Correct selection is automatically assured.

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A NEW -*hp*- AUDIO OSCILLATOR

3 OUTSTANDING NEW FEATURES:

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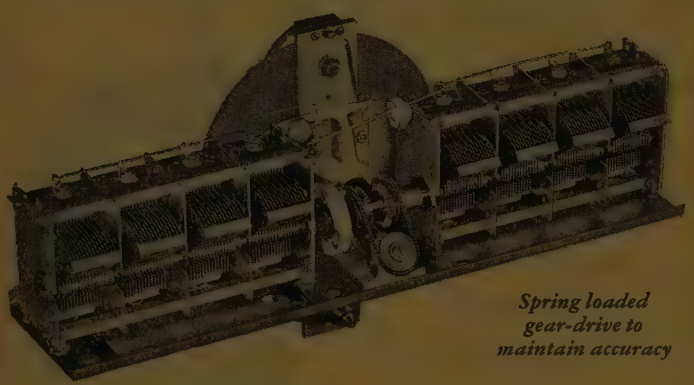
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2. RUGGED CONSTRUCTION

3. IMPROVED CIRCUIT



New friction driven dial provides hair-line accuracy



Spring loaded gear-drive to maintain accuracy

Rugged chassis construction on heavy cast frame



The Model 200-I is a resistance-tuned audio oscillator designed to provide high stability and accuracy for use in frequency measurements. It has a range from 6 cps to 6000 cps,* divided into six frequency ranges as follows: 6 to 20, 20 to 60, 60 to 200, 200 to 600, 600 to 2000 and 2000 to 6000 cps. Each of these ranges has an individual frequency adjustment so that the instrument may be set to a frequency standard such as the -*hp*- Model 100-B.

The large, 6-inch diameter, main frequency dial is calibrated over approximately 300 degrees, making possible a large number of calibrated points to cover the entire range. The dial itself rotates behind a fine wire locator which is visible through an opening in the panel. Parallax is completely eliminated and calibrations are spread over an effective scale length of nearly eight feet. Fast and extremely accurate settings are made easily with this dial. There are two manual controls: one direct action, and the second for vernier adjustments. An electronically regulated power supply is included to assure greatly improved stability.

The Model 200-I -*hp*- Audio Oscillator is but one among many new -*hp*- instruments which are to make public appearance as the cloak of military secrecy is removed. Preliminary technical information is available on this instrument now . . . write for it! And watch for the early release of other new -*hp*- instruments.

**This frequency coverage was selected for interpolation work. Other frequency ranges can be supplied from 6 cps to 100 kc on special order.*

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930

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STACKPOLE

CONTINUOUSLY ADJUSTABLE CARBON RHEOSTATS

(CARBON PILES)

Under impetus of war requirements demanding resistance adjustments for more critical, more precise, smoother, and more dependable control than is possible with conventional variable resistance units, Stackpole engineering has developed the Continuously Adjustable Carbon Rheostat formed of carbon disc piles to a high degree of efficiency. Simply by changing the pressure applied to the pile, every possible resistance value within its range is available without opening the electrical circuit in which it is connected. The pressure to vary the resistance to the most critical adjustment may be applied electrically, mechanically, centrifugally or hydraulically. Uses range from both generator and line voltage regulator applications to remarkable speed control through governed field current on motors. Many other projects incorporating the Carbon Pile Resistor are now in the development stage.

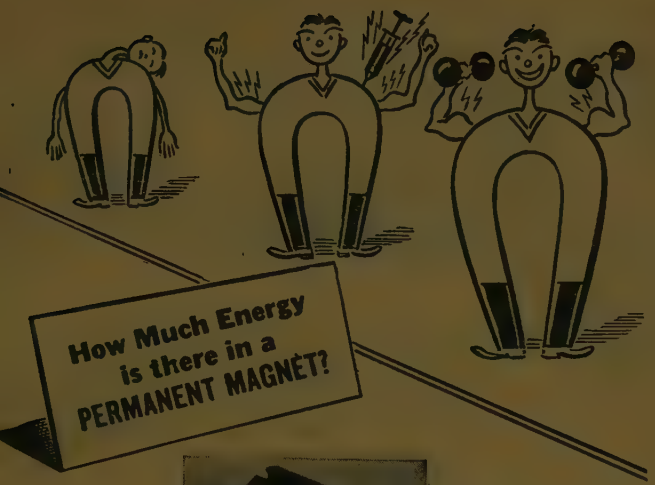
Stackpole regularly supplies Carbon Piles in practically any length pile and diameter required. Resistance ranges have been materially expanded and a greater degree of resistance variation may now be obtained in a pile of a given size. Careful engineering control of manufacture assures a high degree of uniformity on a quantity production basis. Write for bulletin giving complete data.

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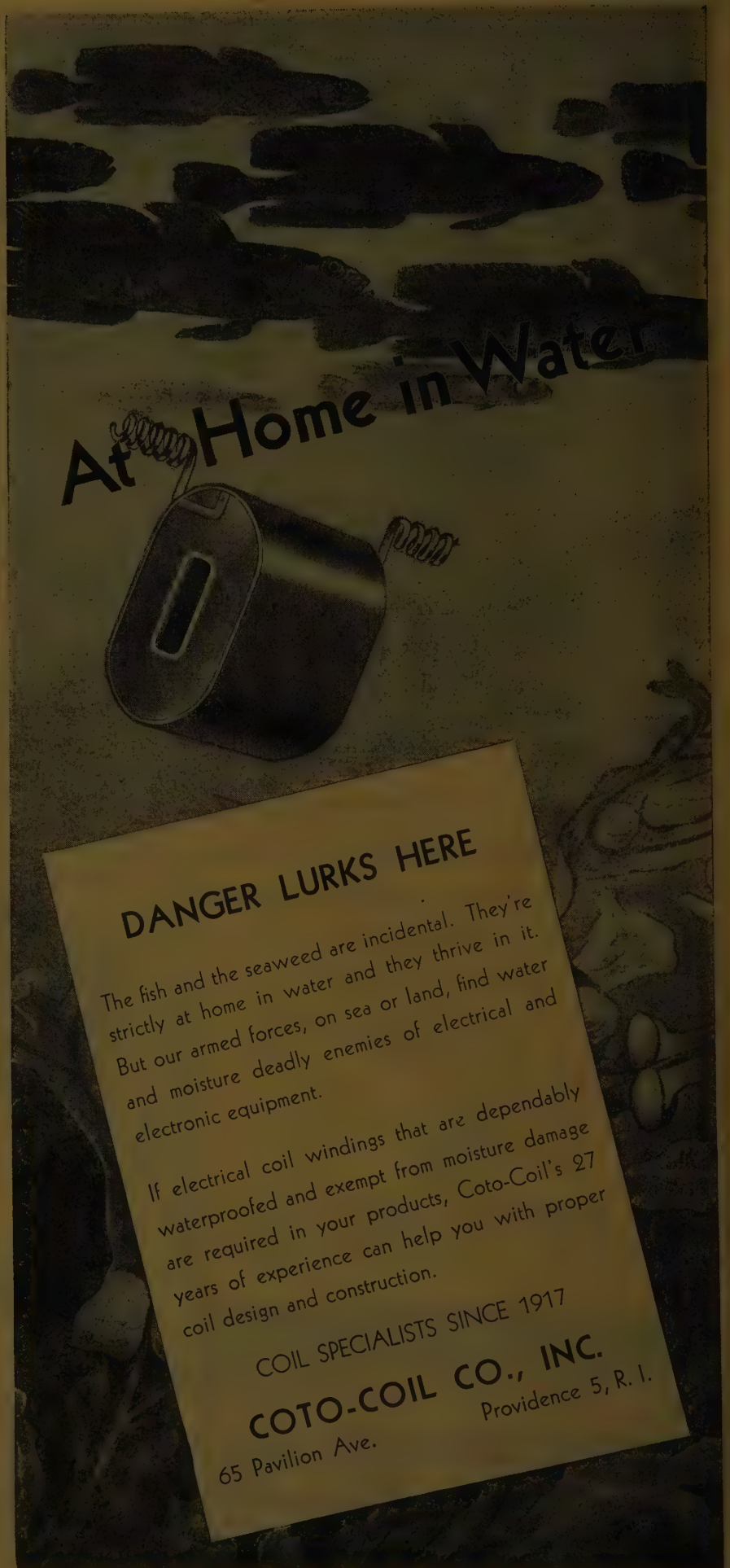
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D.P. D.T. main contacts, rated 6 amps. at 24 volts DC. 3 7/16" long; 1 7/8" high; 1 5/8" wide.



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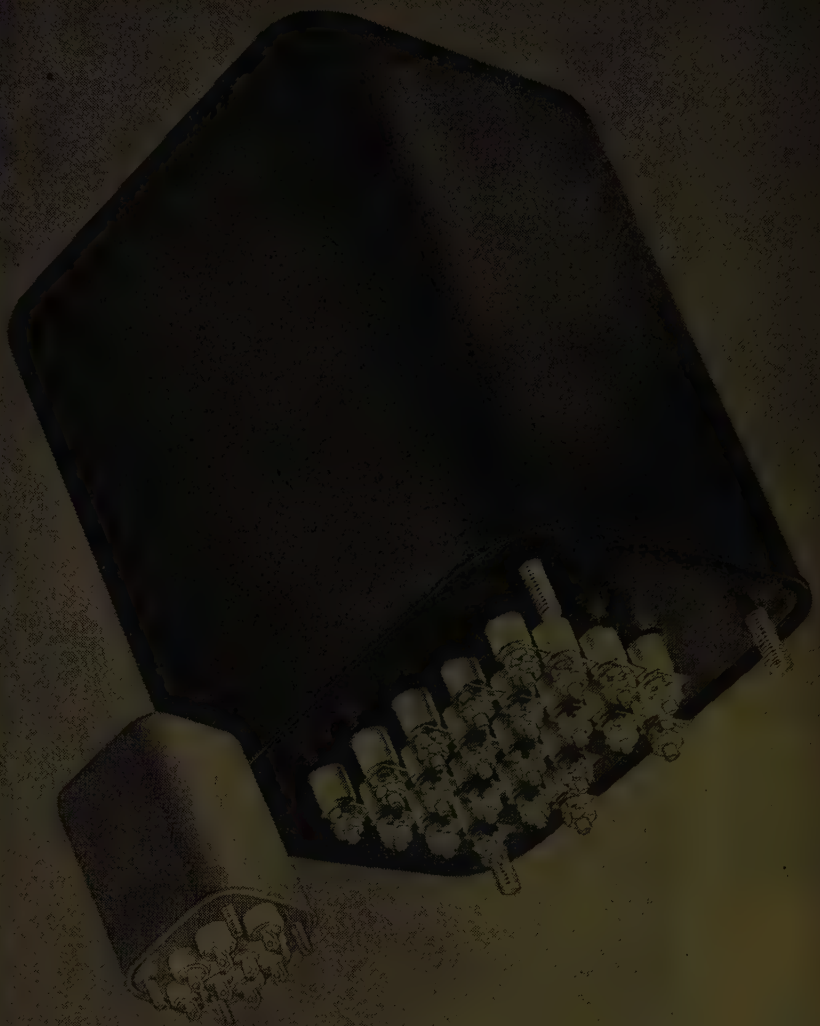
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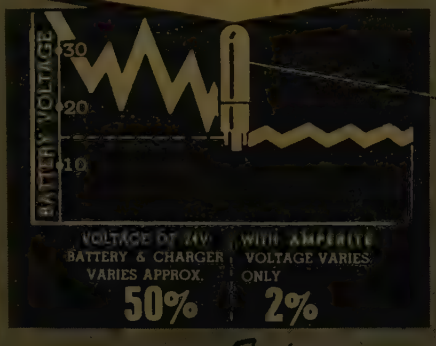
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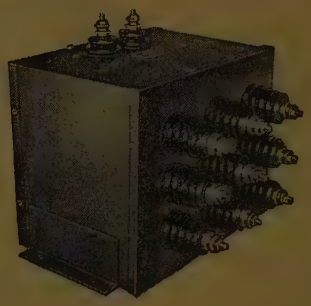


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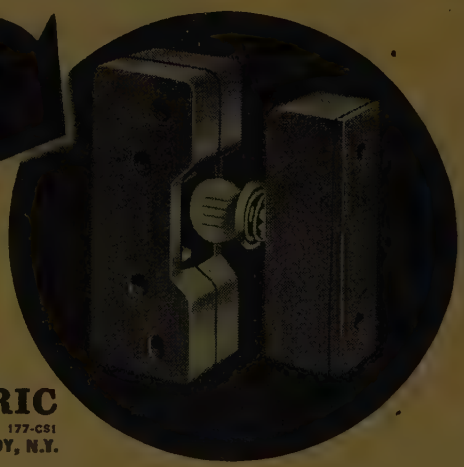


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A graduate electrical engineer, 30 to 40 years of age, possessing supervisory ability, experienced in the design, layout, and specification of electrical equipment for chemical manufacturing plants.

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Give full experience, education, age, present salary and salary expected. Enclose snapshot.

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ENGINEERS. (a) Designer, electrical graduate, 30–40, experienced in the design, layout and specification of electrical equipment for chemical manufacturing plants. Should be well grounded in the fundamentals of electrical theory, and have a working knowledge of practical electrical problems. Location, Delaware. (b) Power, Mechanical or Electrical Engineers, 26–35, with aptitude and experience in heat balance thinking, for work devoted to plant improvements, results engineering and effecting economy in the generation and consumption of steam, electricity, compressed air, water and refrigeration. Locations, throughout the United States. Apply by letter, giving age, experience, education, present and expected salary and enclose a snapshot. W-4632.

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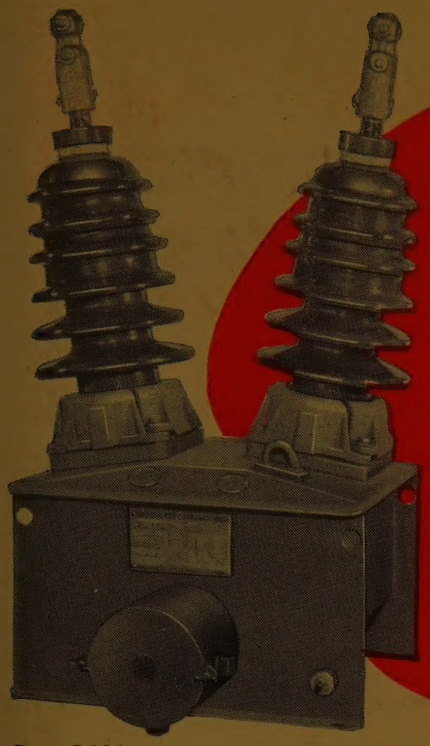
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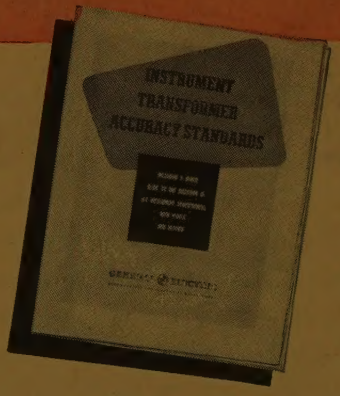
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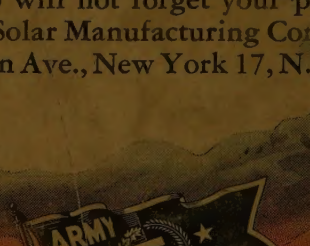
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